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BALLOONING OF CANDU PRESSURE TUBES - MODEL ASSESSMENT

by

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BALLOONING OF CANDU PRESSURE TUBES - MODEL ASSESSMENT (PHASE I)

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ABSTRACT

The transient creep equations used to analyze the possible ballooning and failure of Zr-2.5% Nb pressure tubes during a loss-of-coolant accident (LOCA) were developed and verified using as-received Zr-2.5% Nb pressure tube material. But in a CANDU reactor, the pressure tubes absorb deuterium and are exposed to a continuous neutron fluence. Consequently, a literature survey was done to determine how irradiation damage and deuterium might affect the creep rate and ductility of Zr-2.5% Nb pressure tubes in the temperature range from 600 to 800° C. It was found that irradiation damage, dissolved deuterium and deuteride blisters could possibly affect the creep rate and ductility of ZR-2.5% Nb pressure tubes in this temperature range, but deuteride platelets are expected to have little effect. Further tests are required to determine the effect of irradiation damage and deuterium on the creep rate and ductility of pressure tubes.

RÉSUMÉ

Les équations transitoires de fluage utilisées pour analyser le gonflement et la défaillance des tubes de force de zirconium contenant 2,5 pour 100 de niobium en poids durant un accident de perte de caloporteur ont été mises au point et vérifiées en se servant de matériau de tube de force tel quel. Néanmoins, dans un réacteur CANDU, les tubes de force absorbent du deutérium et sont exposés à une fluence constante de neutrons. On a donc dépouillé la documentation existante pour déterminer comment les dommages attribuables aux rayonnements et le deutérium pouvaient modifier la vitesse de fluage et la ductilité de ces tubes de force à des températures variant de 600 à 800 °C. On a découvert que les dommages attribuables aux rayonnements, le deutérium dissous et les boursouflures de deutériure pourraient modifier la vitesse de fluage et la ductilité de ces tubes de force à des températures équivalentes, mais on ne s'attend pas que les précipités de deutériure aient beaucoup d'effet. D'autres épreuves sont nécessaires pour déterminer l'effet des dommages attribuables aux rayonnements et du deutérium sur la vitesse de fluage et la ductilité des tubes de force.

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1. INTRODUCTION

CANDU[1] nuclear pover reactors use pressure tubes as the primary heat-transport containment. The pressure tubes contain the fuel and hot, pressurized heavy-water coolant, and under normal conditions operate at temperatures between 240 and 300°C, with an internal pressure of about 10 MPa. During some postulated loss-of-coolant accidents (LOCA), the coolant flov through the pressure tubes would be reduced, causing the temperature of the fuel and pressure tubes to rise. It is imperative to be able to determine whether the coolant pressure would cause pressure tube ballooning, since ballooning will influence the heat transfer from the fuel to the moderator, which is a large heat sink. Consequently, a model [1] was developed to predict the ballooning of Zr-2.5% Nb pressure tubes under transient conditions. This model used creep equations [2] developed from data obtained in uniaxial creep tests on specimens machined from as-received Zr-2.5% Nb pressure tubes. The use of the creep equations under transient conditions [3] was verified in uniaxial tests in which the stress was held constant and the temperature was raised at rates up to 50°C/s. The use of the creep equations under biaxial conditions [3] was verified in tests in which sections of as-received Zr-2.5% Nb pressure tubes were internally pressurized and then the temperature of the specimen was raised while the circumference was continuously measured. The model was also verified by doing tests on sections of as-received Zr-2.5% Nb pressure tubes that had a circumferential temperature gradient [4]. The model successfully predicted the average transverse creep strain in these specimens up to failure. The model and the creep equations have thus been thoroughly tested on as-received Zr-2.5% Nb pressure tubes.

A considerable international effort has also been spent in studying the ballooning of fuel cladding during a LOCA. During a LOCA the fuel cladding is internally pressurized, its temperature rises rapidly, and there are variations in temperature around its circumference. Consequently, during a LOCA the fuel cladding balloons under essentially the same conditions as the pressure tubes in a CANDU reactor. The experimental data and models used to predict ballooning of fuel cladding are relevant for pressure tubes, and have recently been revieved [5].

The model and creep equations used to predict the ballooning of Zr-2.5% Nb pressure tubes during a LOCA have been developed and thoroughly verified using specimens of as-received Zr-2.5% Nb pressure tubes. However, pressure tubes in a CANDU nuclear reactor absorb deuterium and are irradiated, and this deuterium and irradiation damage can reduce the ductility of the pressure tubes. During a postulated LOCA the temperature of the pressure tubes can rise rapidly (rates up to 25°C/s), but very little creep deformation occurs belov 650°C. It was assumed that the irradiation damage would anneal out and the deuterides would dissolve at temperatures belov 650°C, and the model developed for as-received Zr-2.5% Nb pressure tubes could therefore be used. The object of this report is to reviev all the relevant available data to determine whether or not this assumption is valid. If the assumption is not valid then further experiments will be recommended.

1/ CANada Deuterium Uranium. Registered in the U.S. Patent and Trademark Office.

2. REVIEW OF THE PRESSURE TUBE BALLOONING MODEL

For LOCA scenarios the pressure tube ballooning model is used to analyze the deformation of pressure tubes. If the conditions during the LOCA are severe enough to cause significant pressure tube ballooning, then the model is used to determine whether the circumferential temperature distribution would cause the pressure tube to fail before it ballooned into contact with its calandria tube. If the pressure tube does not fail, then the model is used to determine the pressure tube temperature when it balloons into contact with its calandria tube. This contact temperature is an important parameter since it determines whether or not the calandria tube will go into dryout after contact [6]. The ballooning model will be revieved to illustrate the important parameters that control the contact temperature and failure of a pressure tube. In the folloving sections I will try to determine hov deuterides and irradiation damage affect these parameters.

Matthevs and Tripp [7] developed a model for the ballooning of internally pressurized thin-valled tubes that have a circumferential temperature variation, and found the tube tended to remain circular even though the deformation varied around the circumference. This vas also verified experimentally [1,31]. Thus it vas assumed that the cross section of the pressure tube would remain circular. This greatly simplified the analysis by reducing the two-dimensional problem to a one-dimensional one. Also it was assumed that the transverse stress vas tvice the axial stress at every point around the tube. Thus the local transverse creep rate vas

$$\dot{\varepsilon}_t = f(T, \sigma_t, t) \tag{1}$$

vhere T is the temperature, σ_t is the transverse stress, and t is the time. The local transverse stress is

$$\sigma_t = Pr/v \tag{2}$$

vhere P is the pressure difference across the vall, r is the tube radius, and v is the local vall thickness.

With these assumptions, the local and average transverse creep strains of a pressure tube that is internally pressurized and has a circumferential temperature variation, vere calculated by dividing the circumference into a number of segments. The lengths of these segments vere small enough so that the temperature could be assumed to be constant over the segments. The creep equation vas numerically integrated for each segment. At the end of each time step, the geometry vas updated by calculating nev segment lengths and vall thicknesses, using the increments in creep strain determined for each segment. In the calculation of the vall thickness, it vas assumed that the volume did not change during creep and the axial creep rate vas negligible. Thus $\Delta\varepsilon_r = -\Delta\varepsilon_t$, vhere $\Delta\varepsilon_r$ and $\Delta\varepsilon_t$ are the increments in radial and transverse creep strains during the time step. The nev tube radius vas found by adding all the segment lengths to find the nev circumference, and dividing by 2π. This procedure vas repeated until the pressure tube either failed or contacted its calandria tube.

At high temperatures, as-received Zr-2.5% Nb pressure tubes are ductile, and failure occurs by necking down to a chisel edge. Consequently, the pressure tube ballooning model has an upper and a lower bound for failure, and for both failure criterion it is assumed that the critical segment necks down to zero wall thickness. For the upper bound criterion, the wall thickness is assumed to be uniform and failure occurs at a local hot spot where the tube necks down to zero wall thickness. When the critical segment necks down to zero wall thickness, it does so at a point and not along the whole segment, as shown in Figure 1. Assuming the axial creep strain is negligible and the volume does not change during creep, then the length of the critical segment at failure is [1]

$$l_c = \frac{4w_o\, l_o}{w_1 + w_2} \tag{3}$$

where w_0 and l_0 are the initial wall thickness and length of the failed segment, respectively, and w_1 and w_2 are the final wall thicknesses of the adjacent segments. Although failure is assumed to occur when the transverse strain in the critical segment becomes infinite, the length of the critical segment remains finite and is given by Equation (3). For the lower bound failure criterion it is assumed that there are variations in the wall thickness, and there is a defect in the critical segment. This combination of defect and local hot spot cause the tube to neck to zero wall thickness more quickly than if there were just a local hot spot. The average transverse creep strain in the critical segment, when the defect causes it to neck down to zero wall thickness, is [1,8]

$$\varepsilon_f = -\frac{1}{n} \ln \left[1 - \left\{ \frac{w_o - d}{w_o} \right\}^n \right] \tag{4}$$

where w_0 is the initial nominal wall thickness, d is the maximum possible depth of a local defect and n is the creep stress exponent. For the lower bound failure criterion, the average transverse creep strain in the critical segment is not infinite, but is given by Equation (4).

The transverse creep equation for as-received Zr-2.5% Nb pressure tubes, in the temperature range from 450 to 850°C, is [2]

$$\dot{\varepsilon}_t = 1.3 \times 10^{-5}\, \sigma_t^9 \exp\left(-\frac{36\,600}{T}\right) + \frac{5.7 \times 10^7\, \sigma_t^{1.8} \exp\left(-\frac{29\,200}{T}\right)}{\left[1 + 2 \times 10^{10} \int_{t_1}^{t} \exp\left(-\frac{29\,200}{T}\right) dt\right]^{0.42}} \tag{5}$$

where $\dot{\varepsilon}_t$ is the transverse creep rate in s^{-1}, σ_t is the transverse stress in MPa, t is the time in s, and t_1 is the time when T = 973 K. The first

term describes the creep rate at temperatures below about 600°C, and for stresses above about 200 MPa. At temperatures above 600°C, there is a change in the creep mechanism in Zr-2.5% Nb, which reduces the creep strength, and Zr-2.5% Nb becomes superplastic. The second term in the creep equation describes the creep rate in this superplastic region. This change in the creep mechanism is fortunate, as it allows as-received Zr-2.5% Nb pressure tubes to balloon into contact with their calandria tubes at fairly low temperatures as a result of the decrease in creep strength above 600°C.

The transverse creep equation for as-received Zr-2.5% Nb pressure tubes in the temperature range 850 to 1200°C is [2]

$$\dot{\varepsilon}_t = 10.4\ \sigma_t^{3.3} \exp\left(\frac{-19\,600}{T}\right) + \frac{3.5 \times 10^4\ \sigma_t^{1.4} \exp\left(\frac{-19\,600}{T}\right)}{1 + 274 \int_{t_2}^{t} \exp\left(\frac{-19\,600}{T}\right) (T - 1105)^{3.72}\, dt} \qquad (6)$$

where t_2 is the time when T = 1123 K. The first term describes the power law creep in the β-phase, and the second term describes the grain-boundary sliding component. The grain-boundary sliding component is significant at temperatures between about 850 and 950°C, when the α-phase to β-phase transformation is rapid.

These transverse creep equations were developed using data from uniaxial creep tests in which the temperature and stress were held constant; in which the temperature was held constant and the stress was varied in steps to determine the stress exponent; and in which the stress was held constant and the temperature was varied in steps to determine the activation energy [2]. These transverse creep equations were then verified using transient uniaxial and biaxial tests [3]. For the transient uniaxial tests, stresses from 5 to 130 MPa and temperatures ramp rates from 1 to 50°C/s were used. For the transient biaxial tests, internal pressures from 0.5 to 10 MPa were used, and the temperature ramp rate was about 5°C/s below about 500°C and it dropped to about 1°C/s above 800°C. If a pressure tube balloons uniformly, it will come into full contact with its calandria tube at a transverse creep strain of about 18%. Consequently, the creep equations were used to predict the pressure-tube temperature at which the transverse creep strain would be 18% (contact temperature) in the verification tests, and this was compared to the measured contact temperature. For contact temperatures below 850°C, the maximum difference in the measured and predicted contact temperatures was 35°C [3]. This illustrates that for the limited material used in the verification tests, the creep equations predicted the contact temperature fairly accurately. However, there is not sufficient creep data to accurately determine the creep rate distribution for all the Zr-2.5% Nb pressure tubes now used in CANDU reactors.

The uniaxial tests used for developing the creep equations, and for verification, were done in an argon atmosphere; whereas the biaxial

tests were done in a nitrogen atmosphere. At temperatures below 850°C, there was good agreement between the predicted and measured contact temperatures for the transient biaxial tests. However, for a transient biaxial test with an internal pressure of 0.5 MPa, the predicted contact temperature was 870°C, whereas the measured contact temperature was 1020°C [3]. This result suggested that the nitrogen atmosphere decreased the creep rate above about 850°C. This was checked by doing uniaxial tests in both an argon and nitrogen atmosphere, using an applied stress of 5 MPa and a temperature ramp rate of 1°C/s. The contact temperature was 840°C for the argon atmosphere, and 1060°C for the nitrogen atmosphere. This verified that a nitrogen atmosphere decreased the creep rate of Zr-2.5% Nb pressure tubes above about 850°C. An oxygen atmosphere would also be expected to decrease the creep transverse rate at temperatures above about 850°C.

The ballooning model was used to calculate the transverse creep strain that would occur in an as-received Zr-2.5% Nb pressure tube with an internal pressure of 10 MPa for a temperature ramp of 25°C/s. The results are given in Figure 2. As 10 MPa is the maximum possible internal pressure that could occur in a pressure tube during a LOCA, these results illustrate that there would be very little pressure tube deformation below 650°C during a LOCA, when the temperature is ramped at 25°C/s.

When an irradiated pressure tube with deuterides is heated above the normal operating temperature (about 300°C), the irradiation damage starts to anneal out and the deuterides start to dissolve. For irradiation damage or deuterides to have any effect on the ballooning of pressure tubes during a LOCA in which the pressure tube temperature is ramped at 25°C/s, there must be some irradiation damage or deuterides remaining above 650°C. If all the deuterides dissolve, the dissolved deuterium could still possibly affect the creep rate. If the irradiation damage, deuterides, or dissolved deuterium affect the creep rate of Zr-2.5% Nb above 650°C, then this will obviously affect the contact temperature.

When a pressure tube balloons with a temperature gradient, it may fail before contacting its calandria tube as a result of local straining at a hot spot. We will now review the factors in the creep equations that irradiation damage, deuterides, or dissolved deuterium may affect, and how these parameters would affect failure. For the Norton creep equation [30],

$$\dot{\varepsilon} = A\,\sigma_t^n \exp\left(\frac{-Q}{RT}\right) \tag{7}$$

where A is a constant, n is the stress exponent, Q is the activation energy, and R is the gas constant, the average transverse creep strain at failure for a thin-walled tube ballooning with a circumferential temperature distribution is [5]

$$\varepsilon_t^{av} = -\ln\left\{\frac{1}{2\pi}\int_0^{2\pi}\left[1 - \frac{A(\phi)}{A_{max}}\right]^{1/n} d\phi\right\} \tag{8}$$

where

$$A(\phi) = A \exp\left[\frac{-Q}{RT(\phi)}\right]$$

$$A_{max} = A \exp\left[\frac{-Q}{RT_{max}}\right]$$

and $T(\phi)$ is the temperature distribution around the circumference, ϕ is the angular position, and T_{max} is the maximum temperature. In developing this equation it was assumed that failure occurred when the tube necked to zero wall thickness at the hottest point, and the temperature distribution did not vary with time. The following temperature distribution was used to determine how the stress exponent, n, and Q/R affect the average failure strain:

$$T(\phi) = 700 + 25\ (1 + \cos\phi)\ ^{\circ}C \tag{9}$$

As the creep Equation (5) is quite complicated, it was simplified to

$$\dot{\varepsilon}_t = 5.7 \times 10^7\ \sigma_t^{1.8} \exp\left(-\frac{29\ 200}{T}\right) \tag{10}$$

to illustrate how varying n and Q/R affect the average transverse creep strain at failure. Creep Equation (10) is valid for transverse stresses below 200 MPa and for high heating rates (greater than 10°C/s). First, the value of Q/R was held constant at 29 200 and n varied from 1 to 10 to see how the stress exponent affected the average transverse strain at failure, and the results are given in Figure 3. It is seen that average transverse strain at failure decreases with increasing stress exponent. Next, n was held constant at 1.8, and Q/R was varied from 10 000 to 50 000, and the results are given in Figure 4. The average transverse strain at failure decreases with increasing value of Q/R. Thus if irradiation damage, deuterides, or dissolved deuterium affect the stress exponent, n, or Q/R, this could affect the average transverse strain at failure.

Irradiation damage, deuterides or dissolved deuterium may affect the ductility of a ballooning Zr-2.5% Nb pressure tube. Consequently the ballooning model was used to determine how varying the strain at which the critical element failed (local failure strain) would affect the average transverse strain at failure. The temperature distribution given by Equation (9) was used in the analysis. The results obtained for internal pressures of 10, 5, and 2 MPa are given in Figure 5. For internal pressures of 10 and 5 Mpa the average transverse strain at failure was sensitive to local failure strains less than about 1, but not to local failure strains greater than 1. For an internal pressure of 2 Mpa the average transverse strain at failure was sensitive to local failure strains less than about 1.7, but not to local failure strains greater than 1.7. Thus if the remaining irradiation damage, deuterides or dissolved deuterium do not lower the local failure strain below about 1.0, there should be no reduction in the average transverse strain at failure, for internal pressures greater than 5 MPa. High-pressure LOCA scenarios are the most likely to be affected by irradiation damage or deuterides, since the pressure tube deformation occurs at lower temperatures. If there is any

irradiation damage or deuterides remaining when the pressure tube starts to deform during a low-pressure LOCA scenario, the local failure strain would have to drop below about 1.7 to affect the average transverse strain at failure.

3. HYDROGEN AND DEUTERIUM

As hydrogen and deuterium have the same effect on the mechanical properties of zirconium alloys and the same dissolution rate, hydrogen and deuterium, and hydride and deuteride, will be used interchangeably in this report. But the atomic number of deuterium is twice that of hydrogen; consequently 2x wt% deuterium has the same effect on the mechanical properties of zirconium alloys as x wt% hydrogen.

3.1 DISSOLUTION OF HYDRIDES

The earliest work on dissolution of hydrides was on Zircaloy-4 fuel sheathing [9]. Kearns used hydrogen concentrations of 60, 114, and 205 ppm. Specimens, 0.5 mm thick, were heated in a lead bath, then quenched into ice brine. Dissolution time was taken as that required for complete disappearance of the original hydride platelets as revealed by optical metallography; the dissolved hydrides reappeared as a much finer precipitate and were clearly distinguishable from those that did not dissolve. Kearns also devised a fairly simple mechanistic theory to compare against experimental data. The results, both for experiment and theory, are shown in Figure 6. At 450°C, dissolution was complete in about 2 s. With the experimental techniques used, it is clear that there would be little point in trying to establish data at shorter heating times.

Kearns' experiment was repeated at Chalk River Nuclear Laboratories (CRNL) using specimens from cold-worked Zr-2.5% Nb pressure tubes. When it became apparent that the results for this alloy would differ from those reported by Kearns, his work on Zircaloy-4 was repeated [10]. Good agreement with Kearns' results was achieved, as shown in Figure 7, giving credence to the results on Zr-2.5% Nb. Work in India [11] also confirmed Kearns' results on Zircaloy. Another factor revealed in the work at CRNL was that the metallographic technique is not as precise for Zr-2.5% Nb as for Zircaloy-4. Dissolved hydride platelets in Zr-2.5% Nb appear to leave a more distinct residual "shadow" and the determination of when the hydride had truly dissolved involved a large judgement factor. The CRNL work was also extended to specimens heated and quenched while stressed. A collection of the data is shown in Figure 8. In this figure there is data for Zircaloy-4 and Zr-2.5% Nb for hydrogen concentrations from 30 to 124 ppm and tensile stresses from 128 to 276 MPa, and for stressed Zr-2.5% Nb having hydride platelets oriented with their normals parallel to the stress. The figure illustrates the following behaviour in Zr-2.5% Nb:

- when the hydrogen concentration is as low as 30 ppm, and there is no external tensile stress, there is little difference between the hydride dissolution rate in Zircaloy-4 and Zr-2.5% Nb;

- at hydrogen concentrations $\geq$ 60 ppm dissolution rates are significantly slower in Zr-2.5% Nb than in Zircaloy-4;

- a uniaxial tensile stress of 276 MPa can markedly reduce the rate of dissolution; e.g. with 30 ppm the dissolution time at 400°C was possibly 50 times as long as in the unstressed material.

However, it seems evident that for the conditions studied the dissolution times at temperatures $\geq$ 500°C will be very short, $\leq$ 1 s.

All the above tests were done with hydride platelets having thicknesses in the range 0.005-0.03 mm. There is one report [12] on the dissolution of hydride blisters from the surface of cold-worked Zr-2.5% Nb pressure tubes. The tubes were stressed circumferentially at about 150 MPa during dissolution at 280°C. As expected, the time for dissolution was significantly longer than for the smaller hydrides, requiring a few weeks in tubes having 20 ppm hydrogen to > 150 days when the concentration was about 45 ppm. There is no data at higher temperatures, and the best method to obtain blister dissolution rates at higher temperatures would be to do some experiments.

In summary, as the dissolution rate of hydrides does not depend on specimen size, hydride precipitates normally found in cold-worked Zr-2.5% Nb pressure tubes would dissolve in a second or less at temperatures greater than 500°C. Hydride blisters probably will require significantly longer times for dissolution, and if this is to be an issue then further experiments are required to determine the dissolution times at temperatures of interest.

3.2 HYDROGEN EFFECTS ON TENSILE PROPERTIES

The effect of hydrogen on the tensile properties of zirconium alloys has been the subject of numerous studies. Most of this work was on Zircaloy-2 and Zircaloy-4, but only the classic paper of Burton [13] will be revieved here. Burton did tensile tests on Zircaloy-2 specimens containing up to 5000 ppm [H], and at temperatures up to 400°C. The effect of hydrogen on the ultimate tensile strength (UTS) of Zircaloy-2 is summarized in Figure 9. Hydrogen does strengthen Zircaloy-2, but the effect is greatest at the lower temperatures. At 400°C, the UTS did not vary significantly for hydrogen concentrations up to 5000 ppm. The reduction in cross-sectional area at failure was also measured as a function of temperature and hydrogen concentration, and the results are given in Figure 10. Hydrogen does reduce the reduction in area at failure, but again the effect is greatest at the lower temperatures. At 400°C, the addition of up to 2000 ppm [H] does not significantly change the reduction in area at failure, but the addition of 5000 ppm [H] does. Thus up to 2000 ppm [H] does not significantly affect the strength or ductility of Zircaloy-2 at 400°C.

The effect of hydrogen on the ductility of Zr-2.5% Nb was studied by Ells [14]. He found that the ductility depended on the orientation of the hydrides, and the ductility was least when the hydrides were aligned with their normals parallel to the tensile axis. The ductility, as measured by reduction in area at failure, is given in Figure 11 as a function of temperature. The short transverse specimens in this figure had their hydride platelet normals parallel to the tensile axis. At 300°C the reduction in area at failure is the same for the short transverse specimens with both 10 and 100 ppm [H]. As the terminal solid solubility at 300°C is

60 ppm [15], this indicates that at 300°C the radial hydrides are not affecting the ductility. Thus hydrogen should not affect the ductility of Zr-2.5% Nb at temperatures above 300°C, for hydrogen concentrations less than 100 ppm.

3.3 EFFECT OF HYDRIDES ON FRACTURE TOUGHNESS

Fracture toughness is a measure of a material's resistance to unstable crack propagation. The effect of hydrogen (hydrides) on fracture toughness depends strongly on temperature and orientation of the hydrides. The normal orientation for hydrides in pressure tubes is in the circumferential "plane", which means that the platelet normals are in the radial direction. The effect of these hydrides on fracture properties of zirconium alloys is well documented and was first described in a comprehensive way by Watkins et al. [16] in 1969. Figure 12 is an update of that data used in an unpublished assessment of a surveillance tube from the SGHWR by Pickles and his colleagues at Risley, and shows the effect of circumferential hydrides on critical crack length as determined by burst tests on Zircaloy-2. It shows a pseudo ductile-brittle transition behaviour with the transition temperature increasing from below 100°C for 250 ppm [H] to 180°C for 400 ppm [H]. In these tests, the hydrides have no deleterious effect at all on the fracture properties on the upper shelf, even though the test temperature is still well below terminal solid solubility (TSS).

In the early 1980's some concern arose over the fact that some pressure tubes in Whiteshell Reactor 1 (WR 1) were operating at stress levels similar to Pickering reactors, but were permitted to operate with up to several thousand ppm [H]. Some fracture toughness tests were therefore carried out on compact Zr-2.5% Nb specimens containing up to 800 ppm [H]. Because WR 1 was shut down during this period, the test results were never published. The specimens were tested using essentially the procedures described in the new standard [17], and J_R curves were computed. Estimates of the critical crack lengths were made using the crack driving-force curves for an axial through-wall crack in a pressure tube. These are plotted in Figure 13. Again there is a transition between room temperature and 170°C. There is also a suggestion of a decrease in critical crack length (CCL) between 240 and 300°C, although the data is limited. A decrease is expected when the failure mechanism is one of plastic collapse rather than crack extension, and this is the normal (ductile) mode of failure at these temperatures for as-received material.

As the hydride morphology shifts to the radial orientation, there is a very severe ductile-brittle transition in the fracture behaviour of zirconium alloys. This is best shown by laboratory experiments where the morphology has been carefully controlled. Such experiments were recently carried out for Zircaloy-2 at Ontario Hydro Research Division (OHRD) [18] and WNRE [19], and showed an extremely steep transition when the hydrides were predominantly radial. Figure 14 [18] shows the transition for several values of hydride continuity (HCC), which is a measure of the continuity of hydride platelets through the pressure tube wall. If a crack path exists through the wall, which is 100% contained within hydrides, HCC is 1.0; if it is 50% contained, then HCC is 0.5, and so forth. The transition shifts from about 100°C for low HCC to 280°C for HCC approaching 1.0. While the data is plotted for several hydrogen concentrations, the transition depends more on HCC than on the actual hydrogen concentration. Since above 100 ppm

(E) the HCC becomes essentially constant near 1.0 for material in which the hydrides are predominantly radial, and the transition for HCC = 1.0 is at 260-280°C, the transition is not expected to shift much above 300°C even for hydrogen concentrations well in excess of 200 ppm. The question as to how much irradiation may shift this transition in Zr-2.5% Nb remains to be answered. Simpson et al. [19] saw no measurable shift for Zircaloy after nine months irradiation in the NRU reactor at CRNL. However surveillance of the Nuclear Power Demonstration (NPD) reactor [20] showed a shift in the transition temperature over a period of about three years at higher fluences.

The important message here is that even though the material temperature is well below TSS, above the ductile-brittle transition hydrides do not participate in the fracture process in a measurable way. For hydrogen concentrations well above those at which normal operation of CANDU reactors is permitted, this transition temperature, even for 100% radial morphologies, is below 300°C. Therefore, we feel it is unlikely that hydrides, even if retained under an accident scenario, could affect the ductility at temperatures above 650°C.

4. IRRADIATION DAMAGE

4.1 RECOVERY OF IRRADIATION DAMAGE IN PRESSURE TUBES

Results from tensile tests on annealed Zircaloy-2 have shown that the effects of irradiation to 8×10^{23} n/m^2 at 50 and 280°C were completely removed after one hour at 375 and 425°C, respectively [21]. Half the strengthening from the lower temperature irradiation was eliminated in 30 min at 325°C, while over 140 min at 325°C was required for the same strength reduction after the higher temperature irradiation.

Although the recovery process is complicated by recrystallization when cold-worked material is heated to high temperature, the two processes can be sufficiently separated to discern trends [22]. Cold-worked Zircaloy-2 fuel sheathing from fuel bundles irradiated in Pickering were heated to between 700 and 900°C at rates of 25 to 200°C/s, with hold times up to 5 s, then cooled to room temperature at about 50°C/s. The tubing was then pressurized to fracture at room temperature. The yield and ultimate strengths were similar to those of unirradiated tubes that had experienced the same heat treatment in the range 750 to 820°C. The unirradiated specimens had considerable recrystallization, and the irradiated material was assumed to behave the same way. These results suggest that irradiation damage in Zircaloy-2 is removed within a few seconds at temperatures above 750°C.

Irradiation damage seems to anneal out less easily in Zr-2.5% Nb than in Zircaloy-2. In quenched [23], and quenched and aged [23,24], or annealed condition [24], the strength was reduced to the value of the unirradiated material after one hour at between 550 and 600°C. With quenched, and quenched and aged materials, there is also a softening contribution from overaging, but this appears to start above about 550°C.

Two experiments have been performed on specimens machined from cold-worked Zr-2.5% Nb pressure tubes removed from power reactors [25,26]. In the first experiment the degree of softening was measured after several days at temperatures up to 80°C higher than the temperature of irradiation. In the second experiment annealing temperatures up to 600°C for times up to one hour were used. The recovery was defined as:

$$\% \text{ recovery} = \frac{\text{strength as-irradiated - strength after anneal}}{\text{strength as-irradiated - strength unirradiated}} \times 100$$

In this definition the recovery of cold-work is ignored. The time taken to achieve a certain degree of recovery was interpolated from the raw data and plotted versus reciprocal temperature, and the results were extrapolated to 800°C (Figure 15). Although there is much scatter in the data, an interpolation between the low- and high-temperature anneals seems to be reasonable. If the extrapolation is valid, 90% of the irradiation strengthening is recovered in about 550 s at 600°C, 50 s at 700°C, and 6 s at 800°C.

4.2 THE EFFECT OF IRRADIATION DAMAGE ON THE TENSILE PROPERTIES

Tensile specimens have been cut from pressure tubes removed from a CANDU reactor after operating for up to 12 years [27]. Tensile tests were done in both the axial and the transverse direction. For the tensile tests in the transverse direction, ring specimens were used. The measured ultimate tensile strength (UTS) at 302°C as a function of fluence is given in Figure 16. Most of the strengthening seems to have occurred by a fluence of 7×10^{24} n/m^2. At this temperature the irradiation increased the UTS in the axial direction by 39%, and by only 23% in the transverse direction. The measured reduction in cross-sectional area at failure for tests at 302°C is given in Figure 17 as a function of fluence. Again the reduction in area at failure does not change significantly after a fluence of 7×10^{24} n/m^2. The measured UTS for both irradiated and unirradiated specimens is given in Figure 18 as a function of temperature. The strengthening due to the irradiation was negligible above about 475°C. The measured reduction in area at failure is given in Figure 19 as a function of temperature. Again the reduction in ductility due to irradiation damage was negligible above about 475°C. When these tests were done it took 1/2 to 2 hours to reach test temperature, and the specimens were at test temperature for about 15 to 20 minutes before testing. Thus a significant amount of the irradiation damage was probably annealed out before testing. If the specimens had been heated at a rate of 25°C/s, as could occur during a LOCA, the amount of irradiation damage that would have annealed out before testing would have been less, and probably irradiation damage would have had an effect on the UTS and ductility at higher temperatures. Consequently, these results can not be used to determine whether irradiation damage will affect the strength or ductility above 475°C for specimens that are heated rapidly (25°C/s).

4.3 THE EFFECT OF IRRADIATION DAMAGE ON FRACTURE PROPERTIES

The fracture toughness of irradiated Zr-2.5% Nb pressure tubes removed from CANDU reactors has been measured using 17-mm curved compact specimens [17,28]. The data from these tests are used to calculate a J_R curve. The critical crack length (CCL) is calculated using this J_R curve and the crack-driving force for an axial crack in a pressure tube. The

results for all the calculated CCL's, for the temperature range 240 to 300°C, are given in Figures 20 and 21 [29]. The results in Figure 20 for the outlet end, and in Figure 21 for the inlet end, are essentially the same. But these figures show that, in the temperature range 240 to 300°C, the CCL decreases with increasing fluence, decreasing from a CCL of about 90 mm at a fluence of around 1.0×10^{25} n/m^2, to 45-50 mm at a fluence of 8.0×10^{25} n/m^2. Most of the decrease in the CCL occurs by a fluence of 2.0×10^{25} n/m^2, and then the CCL decreases more slowly with further increases in fluence, but it is difficult to determine whether it has saturated. These results show that irradiation damage decreases the CCL in the temperature range 240 to 300°C, but there is no data at higher temperatures.

5. UNIAXIAL CREEP TESTS ON IRRADIATED ZR-2.5% NB

C.K. Chow, at WNRE, performed uniaxial creep tests in the transverse direction on samples machined from Pickering-3 pressure tube J4, which had received a fluence of $1. \times 10^{25}$ n/m^2. The results from these tests were not published. Before the transverse creep specimens were machined, ring sections from the pressure tube were heated to 250°C so they could be flattened without cracking. In these uniaxial creep tests the initial stress was 140 MPa, and the load was held constant while the specimen temperature was ramped by passing an electric current through it. The temperature was measured with a thermocouple spot-welded directly on the specimen. The creep strain was measured using four linear variable differential transducers (LVDT) [2]. The results for all these tests are given in Figures 22 to 26, where the measured temperature and transverse creep strain are plotted as a function of time. Although they tried to ramp the temperature at a constant rate, the ramp rate varied because the temperature was controlled by hand. In Figures 22 and 23 the average temperature ramp rate was about 4°C/s; in Figure 24 it was about 11°C/s and in Figures 25 and 26 it was about 19°C/s. Using the measured temperature and load, the creep strains predicted by creep Equation (5) were calculated and are also plotted in Figures 22 to 26. The predicted creep strain agrees very well with the measured creep strain in Figure 22, and reasonably well in Figure 24, but in the other figures the predicted creep strain is much lower than that measured. In section 4.2, irradiation damage was shown to strengthen Zr-2.5% Nb up to about 500°C. Consequently, if irradiation damage does affect the creep rate at higher temperatures, irradiation damage would be expected to decrease the creep rate rather than increase it as found in these tests. As these tests were done about 5 years ago and the experimental details are forgotten, it is difficult to determine whether this discrepancy is real, or whether it is due to experimental difficulties. When a uniaxial specimen is heated directly by an electric current, the resulting deformation is very non-uniform and there can be large temperature gradients along the specimen, because any local necking will cause the local resistance to increase, resulting in a local increase in temperature. This in turn will result in a local increase in the creep rate and very non-uniform deformation. Thus it is essential to measure the temperature and creep strain at the same point. When these tests were done in the Hot Cells it was difficult to tell whether the temperature and creep strain were measured at the same point.

This may have caused an error in the measurement, and hence the discrepancy between the measured and predicted creep strains.

For these creep tests, the temperature ramp rates were significantly below 25°C/s, particularly for temperatures above 600°C. Two tests indicate that the creep equations were successful in predicting the creep strain fairly reliably. In the other three tests, irradiation damage increased the creep rate, rather than decreased it as expected. Consequently, I think the results from these uniaxial creep tests are unreliable. Thus, to conclusively determine whether or not irradiation damage would affect the creep rate in pressure tubes heated at the rate of 25°C/s, the experimental technique would have to be improved, and further tests would have to be done on irradiated specimens.

Burst tests have been done on irradiated Zircaloy-4 fuel cladding that was heated by both direct resistance heating and in-reactor heating [5]. The measured circumferential strain at failure, as a function of the temperature difference around the cladding, agreed quite well for both methods of heating. This provides some evidence that direct resistance heating does not affect the creep deformation of irradiated Zr alloys, and is a viable heating method for transient creep tests on irradiated Zr-2.5% Nb pressure tube specimens.

6. DISCUSSION

The model that is used to predict the ballooning of pressure tubes during a LOCA, was developed using data from uniaxial creep tests on as-received Zr-2.5% Nb pressure tube material. This model was also successful in predicting the creep strain and failure in transient uniaxial and biaxial tests on as-received Zr-2.5% Nb specimens. This ballooning model has thus been verified for as-received Zr-2.5% Nb pressure tubes, for the range of internal pressures and temperature ramp rates that could occur during a LOCA.

The mechanical properties of irradiated Zr-2.5% Nb pressure tubes have been measured, using tests in which the specimens were at temperature from 1 to 2 hours before testing. From these tests, irradiation damage was found to significantly reduce the ductility and strengthen Zr-2.5% Nb pressure tubes at temperatures below about 500°C, but above 500°C there was little effect. During a LOCA, the Zr-2.5% Nb pressure tubes would not start to balloon until the temperature reached about 650°C. Consequently, the ballooning model did not account for possible effects of irradiation damage, as it was assumed that the irradiation damage would have no effect on ballooning. However, the data on the annealing of irradiation damage predict that it would take 550 s to anneal out 90% of the irradiation damage at 600°C, 50 s at 700°C, and 6 s at 800°C. Thus, if a pressure tube heats up at 25°C/s during a LOCA, a large fraction of the irradiation damage would probably remain when the pressure tube starts to balloon at 650°C. There is no reliable data to determine how this irradiation damage would affect the creep rate or the ductility of the pressure tubes at temperatures above 650°C. However, if there was a significant effect, it would probably decrease the creep rate, the ductility, or both. This would result in an increase in the contact temperature, a possible reduction in the average transverse creep strain at failure, or both.

Pressure tubes in CANDU reactors absorb deuterium which affects their mechanical properties. Deuterium and hydrogen have the same effects on the mechanical properties of Zr-2.5% Nb, and 2x wt% deuterium has the same effect as x wt% hydrogen. Hydrogen lovers the ductility in Zr-2.5% Nb at temperatures belov 300°C. However, above 300°C there is ample evidence to shov that hydrogen does not affect the ductility, even with hydrogen concentrations up to 2000 ppm, providing the hydrides are platelets. Also, hydrides dissolve in a second or less at 500°C. Thus, hydride platelets should not affect the ductility of Zr-2.5% Nb pressure tubes during a LOCA because the hydride platelets should dissolve before the pressure tube reaches 650°C, and if they do not, any remaining hydride platelets would not affect the ductility. However, there is no data to determine hov hydride blisters would affect the ductility of a Zr-2.5% Nb pressure tube during a LOCA. If hydride blisters had an effect, they would reduce the ductility, and thus possibly reduce the average transverse strain at failure. Also, there is a slight chance that the dissolved hydrogen would affect the creep rate. If it did, it would reduce the creep rate, and this would increase the contact temperature. During a LOCA, deuteride blisters could thus possibly reduce the ductility of the Zr-2.5% Nb pressure tubes, or possibly the dissolved deuterium could reduce the creep rate and increase the contact temperature.

7. CONCLUSIONS

The ballooning model was developed and verified for the range of internal pressures and temperature ramp rates that could occur during a LOCA, using specimens machined from as-received Zr-2.5% Nb pressure tubes. All the tests were done in an argon or nitrogen atmosphere. However, for a LOCA in which the temperature of the pressure tube rises at 25°C/s, there is evidence that there could be a significant amount of irradiation damage remaining when the pressure tube starts to balloon at about 650°C. The possible effect of this irradiation damage was not taken into account in the ballooning model, and there is no reliable data to shov that this irradiation damage would not affect the ballooning of pressure tubes. There is ample evidence to shov that deuteride platelets would not affect the ductility of Zr-2.5% Nb pressure tube during a LOCA. However, there is no data to determine hov deuteride blisters or dissolved deuterium would affect the creep rate or ductility of pressure tubes during a LOCA. A nitrogen atmosphere significantly decreased the creep rate of Zr-2.5% Nb pressure tubes above about 850°C, and oxygen and other reactive species would probably have a similar effect.

Due to these veaknesses in the ballooning model, there may be significant errors in the model predictions of contact temperature and transverse creep stain at failure, when it is used to analyze the ballooning of end-of-service-life pressure tubes. These errors are most likely to be significant when the temperature ramp rate is high (25°C/s); there are deuteride blisters present; the deuterium concentration is high enough to affect the creep rate; or the predicted contact temperature is above about 850°C.

Before the ballooning model can be used to reliably predict the ballooning of end-of-service-life Zr-2.5% Nb pressure tubes, tests should

be done to determine hov irradiation damage, dissolved deuterium, and deuteride blisters affect the creep rate and ductility of pressure tubes for temperature ramp rates of about 25°C/s. The upper temperature limit of the ballooning model is not known accurately, but it is probably above 850°C. Ballooning occurs below 850° for most LOCA scenerios, and at 850°C and above, sag becomes an important deformation mechanism. Consequently, finding the upper temperature limit of the ballooning model should probably have a lower priority.

8. RECOMMENDATIONS

The effect of both irradiation damage and deuterium on the creep rate and ductility of Zr-2.5% Nb pressure tubes during a LOCA are uncertain. Consequently, these two important technical issues should be resolved so that the ballooning model can be reliably used to predict the ballooning of end-of-service-life pressures during a LOCA.

Tests should be done to determine whether irradiation damage affects the creep rate and ductility of Zr-2.5% Nb pressure tubes sufficiently, to significantly change the contact temperature and average transverse creep strain at failure. Zr-2.5% Nb pressure tubes have a change in creep mechanism at about 600°C, which controls ballooning from 600 to 850°C. Also, the creep rate of Zr-2.5% Nb is extremely anisotropic. Consequently, it is imperative the specimens for these tests be manufactured from Zr-2.5% Nb pressure tubes, and the strain in the proposed tests should be in the transverse direction, as occurs during ballooning. There are two types of tests that meet these requirements: uniaxial tests in the transverse direction, and ballooning tests on sections of pressure tubes. Ring specimens could be machined from irradiated Zr-2.5% Nb pressure tubes for the uniaxial creep tests, and this would eliminate the need to flatten irradiated material. Flattening irradiated material may cause cracking or a change in microstructure. Heating rates of 25°C/s would be achieved by passing an electric current through the specimen. The temperature would be measured with a thermocouple spot-welded directly on the specimen, and the creep strain would be measured using the four LVDT system developed at WNRE. We have most of the necessary equipment for these tests, but some development work on the grips would be required, and the creep strain measuring apparatus would have to be modified for these new grips. With these tests, there would be a large temperature gradient along the gauge length of the specimen. However, the four LVDT system measures the creep strain at a point, and not over the whole gauge length. Consequently, providing the temperature, creep strain and applied stress are measured at the same location, the temperature gradient in the gauge length would not affect the results. Thus, the major problem for these tests is to measure the temperature and creep strain at the same location.

Ballooning tests could be done on sections of irradiated pressure tubes. These specimens would be heated with an induction furnace, which could easily heat the specimens at a rate of 25°C/s. The temperature would be measured with several thermocouples spot-welded around the circumference of the specimen. The average transverse creep strain would be measured by a quartz thread wrapped around the specimen and attached to an LVDT, a technique that was developed at WNRE. The specimen would be internally

pressurized with gas. For these tests, an induction furnace and an apparatus to weld end caps on the specimens would have to be purchased. These tests would require a fair amount of development work, since ballooning tests have not been done in the Hot Cells at WNRE. Temperature measurement is expected to be a major problem for these tests because the induction furnace generates a lot of electrical noise, which could cause large errors in the measured temperature.

The ballooning tests are significantly more expensive, and are fairly risky due to problems in temperature measurement. Therefore, uniaxial creep tests are recommended. The proposed test matrix for the uniaxial creep tests is given in Table 1. The creep equations would be used to predict the transverse creep strain in these tests, using the measured temperature and load. The predicted and measured creep strain would be compared. The predicted and measured contact temperature would also be compared to determine whether any differences in the measured and predicted creep strain are significant. The local transverse creep strain at failure would also be measured to determine whether irradiation damage significantly affects the ductility of the pressure tubes, and consequently, the average transverse creep at failure. The proposed test matrix should determine whether irradiation damage would significantly affect the creep rate and ductility of Zr-2.5% Nb pressure tubes during a LOCA.

Tests should also be done to determine whether dissolved deuterium or deuteride blisters change the creep rate and ductility of Zr-2.5% Nb pressure tubes sufficiently, to significantly change the contact temperature or the average transverse creep strain at failure. Hydrogen and deuterium have the same effect on mechanical properties of Zr-2.5% Nb pressure tubes. Consequently, uniaxial creep tests using as-received Zr-2.5% Nb specimens that have been hydrided, are recommended. Transverse creep specimens would be machined from flattened sections of as-received Zr-2.5% Nb pressure tubes. These specimens would then be hydrided gaseously to 200 ppm [H]. The transient creep tests could be done in the lab using existing equipment. The specimens would be heated by passing an electric current through them. The temperature would be measured with a thermocouple spot welded directly on the specimen, and the creep strain would be measured with the four LVDT system. The proposed test matrix is given in Table 2. Tests would be done using specimens containing hydride platelets and blisters, to determine the effect of both dissolved hydrogen and blisters. These tests are thus fairly cheap, and there would be very little risk. The measured creep strain in these tests would be compared to that predicted using the creep equations. The measured and predicted contact temperature would also be compared to determine whether any differences in the measured and predicted creep strain are significant. The local failure strain would also be measured to determine whether there was a reduction in ductility. These proposed tests should determine whether dissolved deuterium, or deuteride blisters, would significantly change the creep rate or ductility of Zr-2.5% Nb pressure tubes during a LOCA.

TABLE 1

PROPOSED TEST MATRIX FOR UNIAXIAL TRANSIENT CREEP TESTS ON IRRADIATED RING SPECIMENS

Number of Tests	Heating Rate (°C/s)	Initial Transverse Stress (MPa)
2	25	150
2	25	125
2	25	100
2	25	75
2	25	50

TABLE 2

PROPOSED UNIAXIAL TRANSIENT CREEP TESTS ON HYDRIDED AS-RECEIVED ZR-2.5% NB SPECIMENS

Number of Tests	Hydrogen Content (μg/g)	Blisters	Heating Rate (°C/s)	Initial Transverse Stress (MPa)
1	200	NO	25	150
1	200	YES	25	150
1	200	NO	25	125
1	200	YES	25	125
1	200	NO	25	100
1	200	YES	25	100
1	200	NO	25	75
1	200	Yes	25	75
1	200	NO	25	50
1	200	YES	25	50

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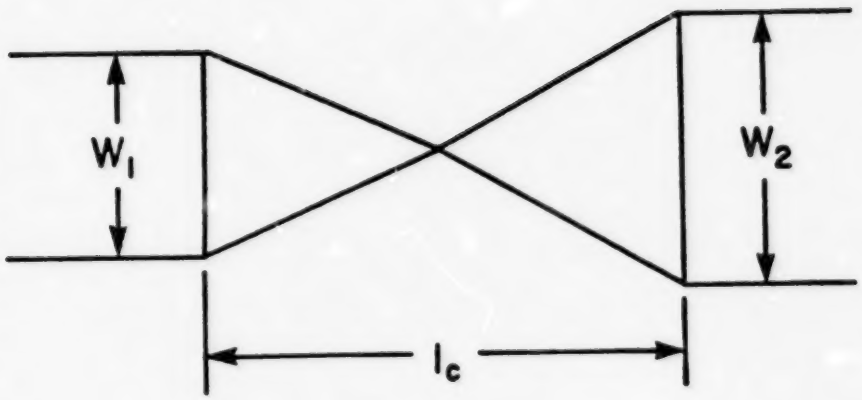


Figure 1: Schematic of the Critical Segment at Failure

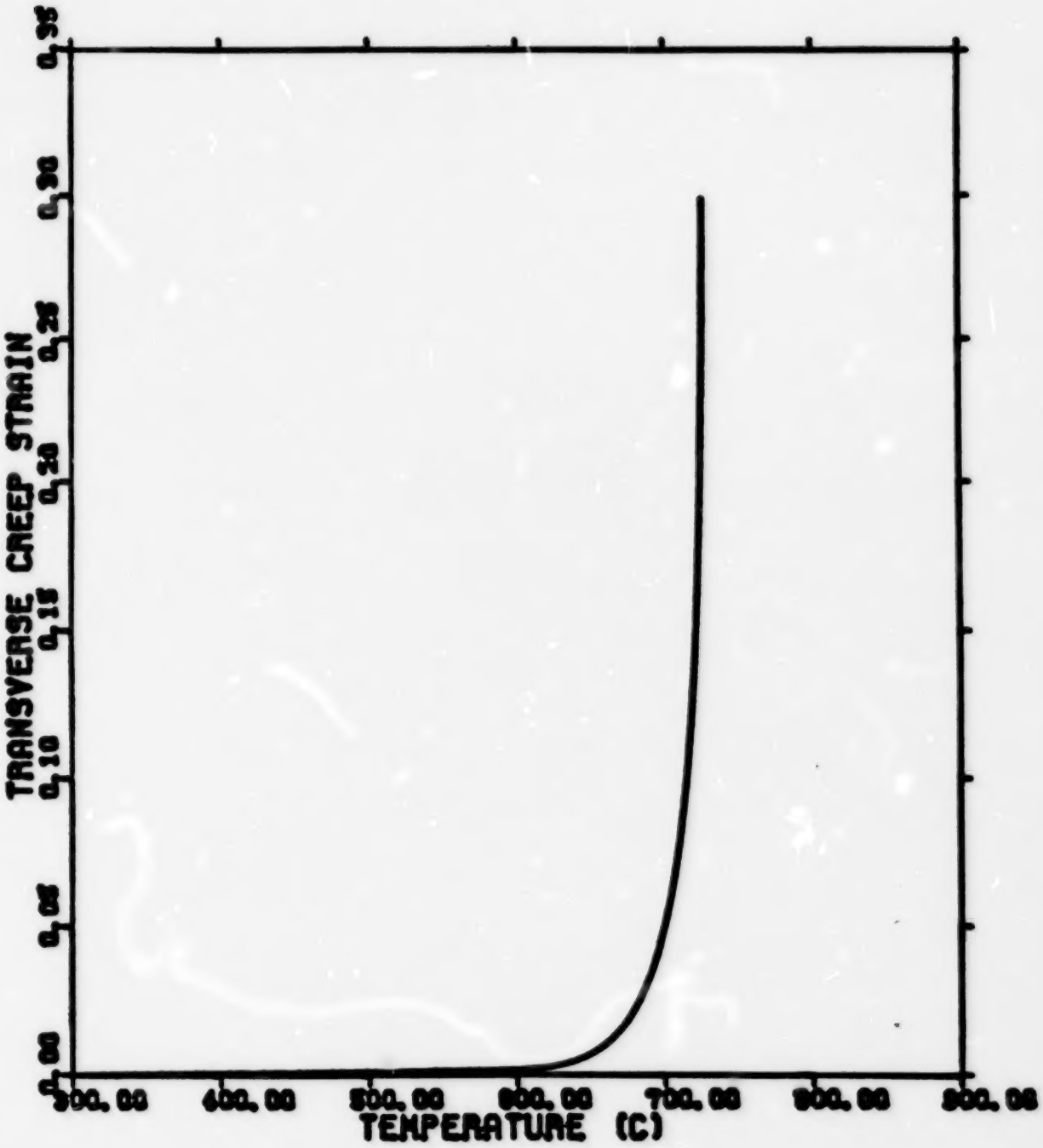


Figure 2: Predicted Transverse Creep Strain in a zr-2.5% nb Pressure Tube with a Temperature Ramp of 25°C/s and an Internal Pressure of 10 MPa

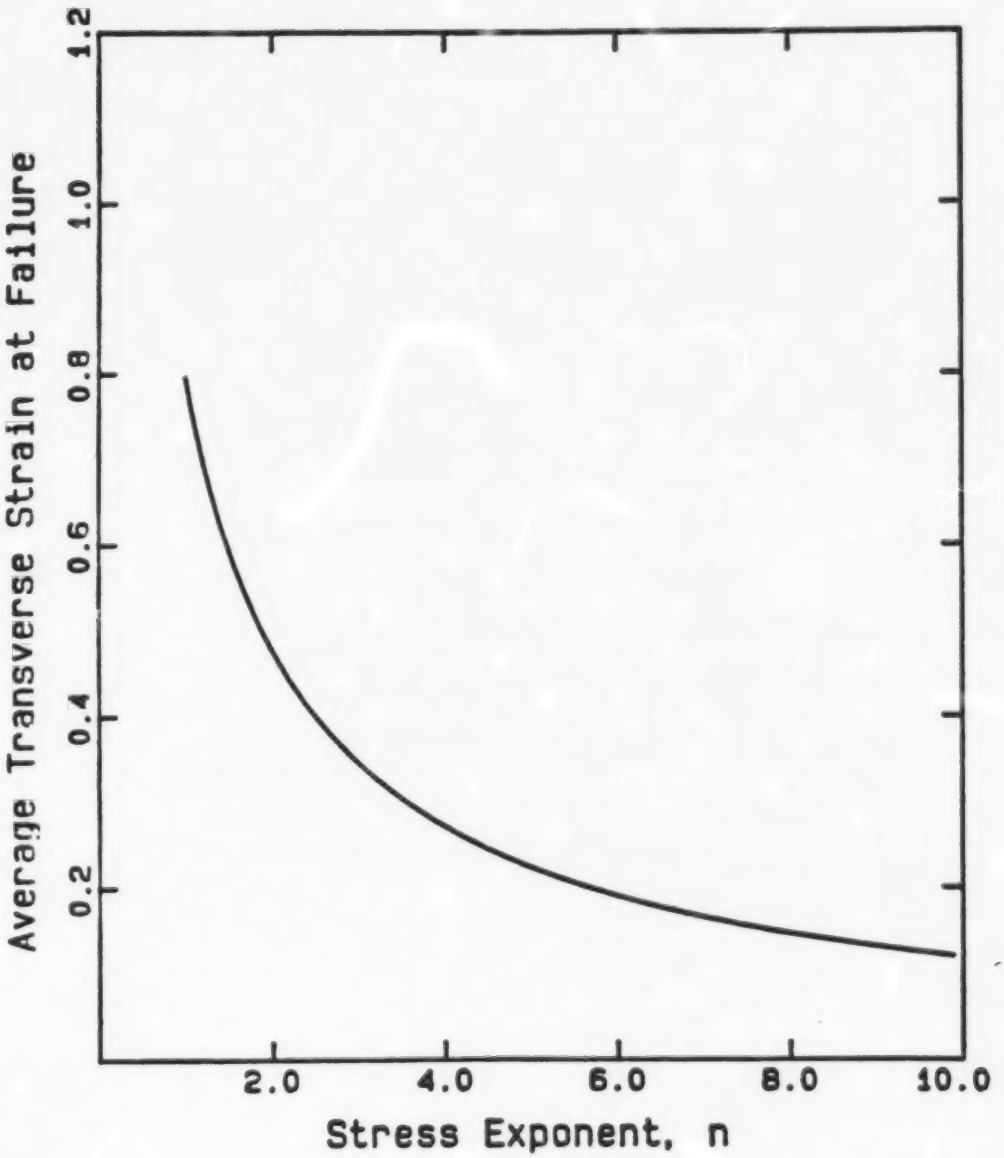


Figure 3: Predicted Average Transverse Creep Strain at Failure of a Pressure Tube, with a Circumferential Temperature Distribution, as a Function of the Stress Exponent, n

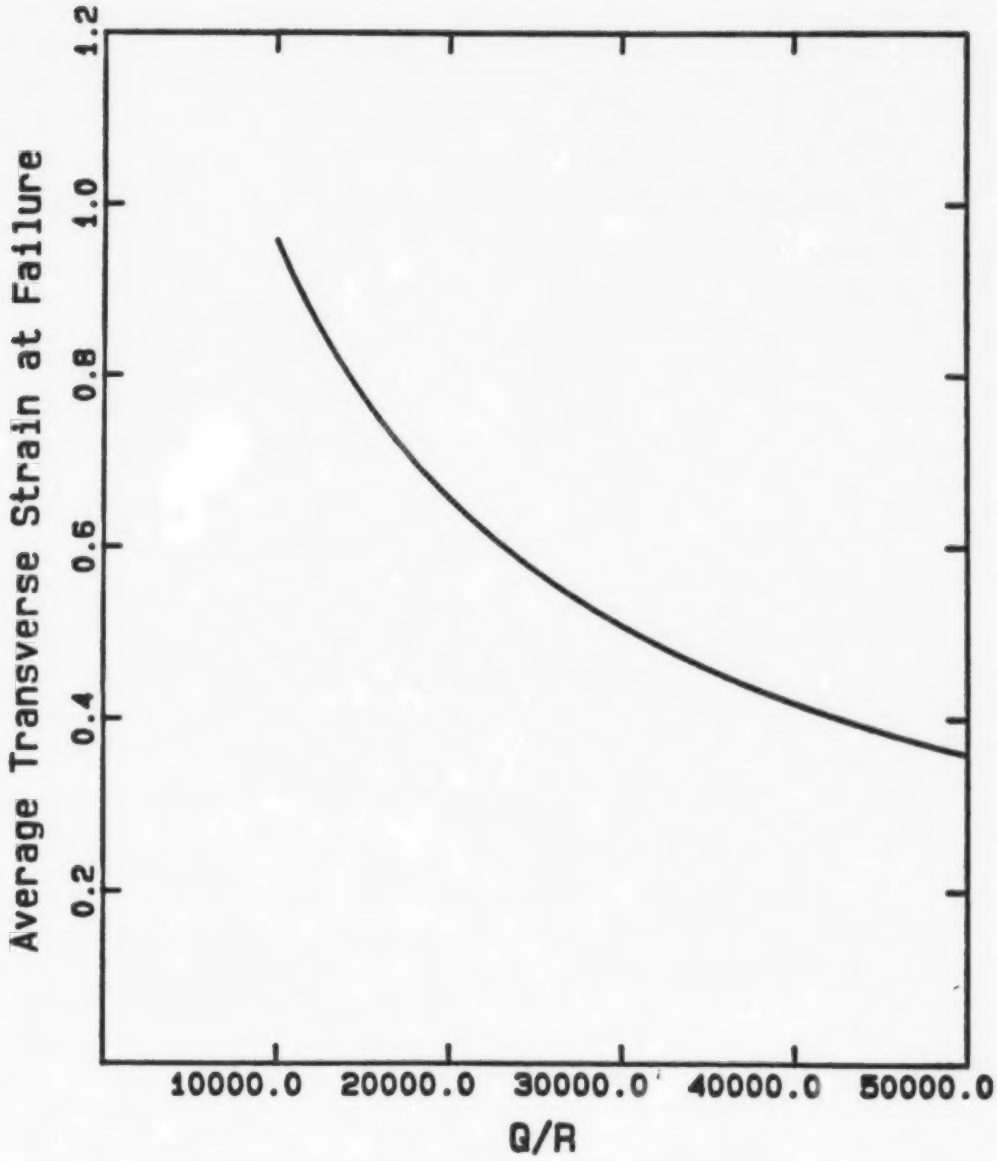


Figure 4: Predicted Average Transverse Creep Strain at Failure of a Pressure Tube, with a Circumferential Temperature Distribution, as a Function of Q/R

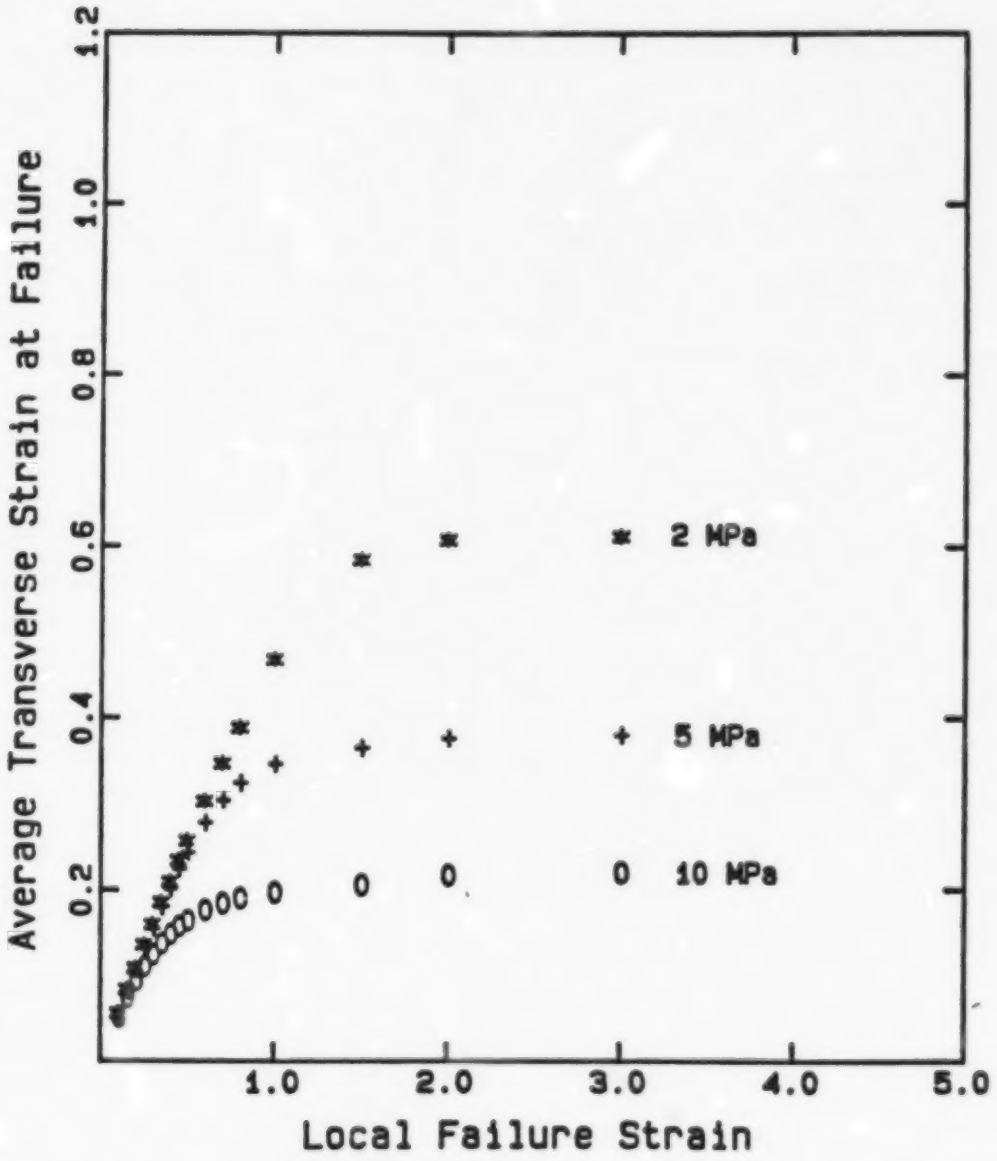


Figure 5: Predicted Average Transverse Creep Strain at Failure of a Pressure Tube, with a Circumferential Temperature Distribution, as a Function of Local Failure Strain

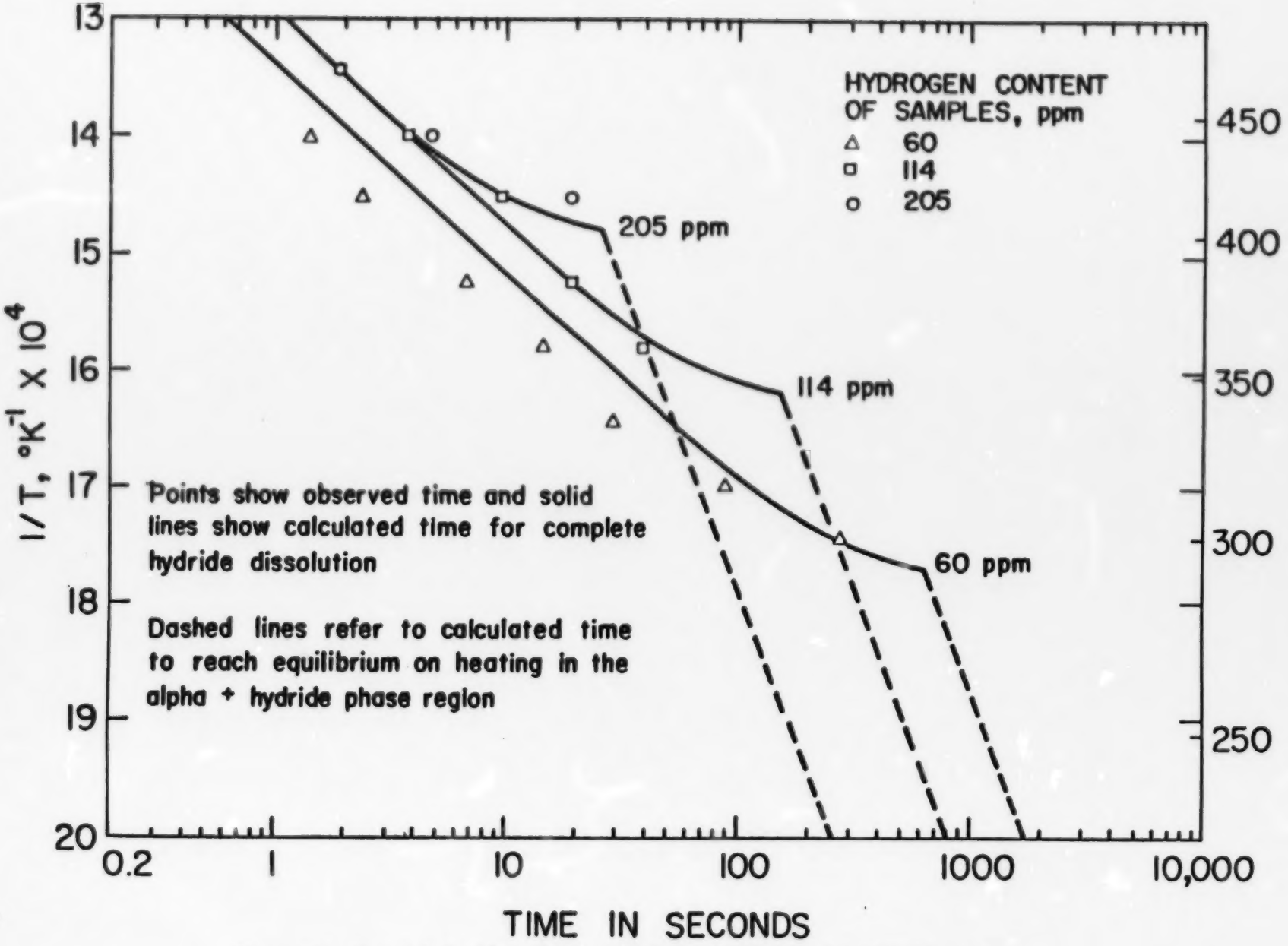


Figure 6: Kearns' Results on Dissolution of Hydrides in Zircaloy-4 [9]

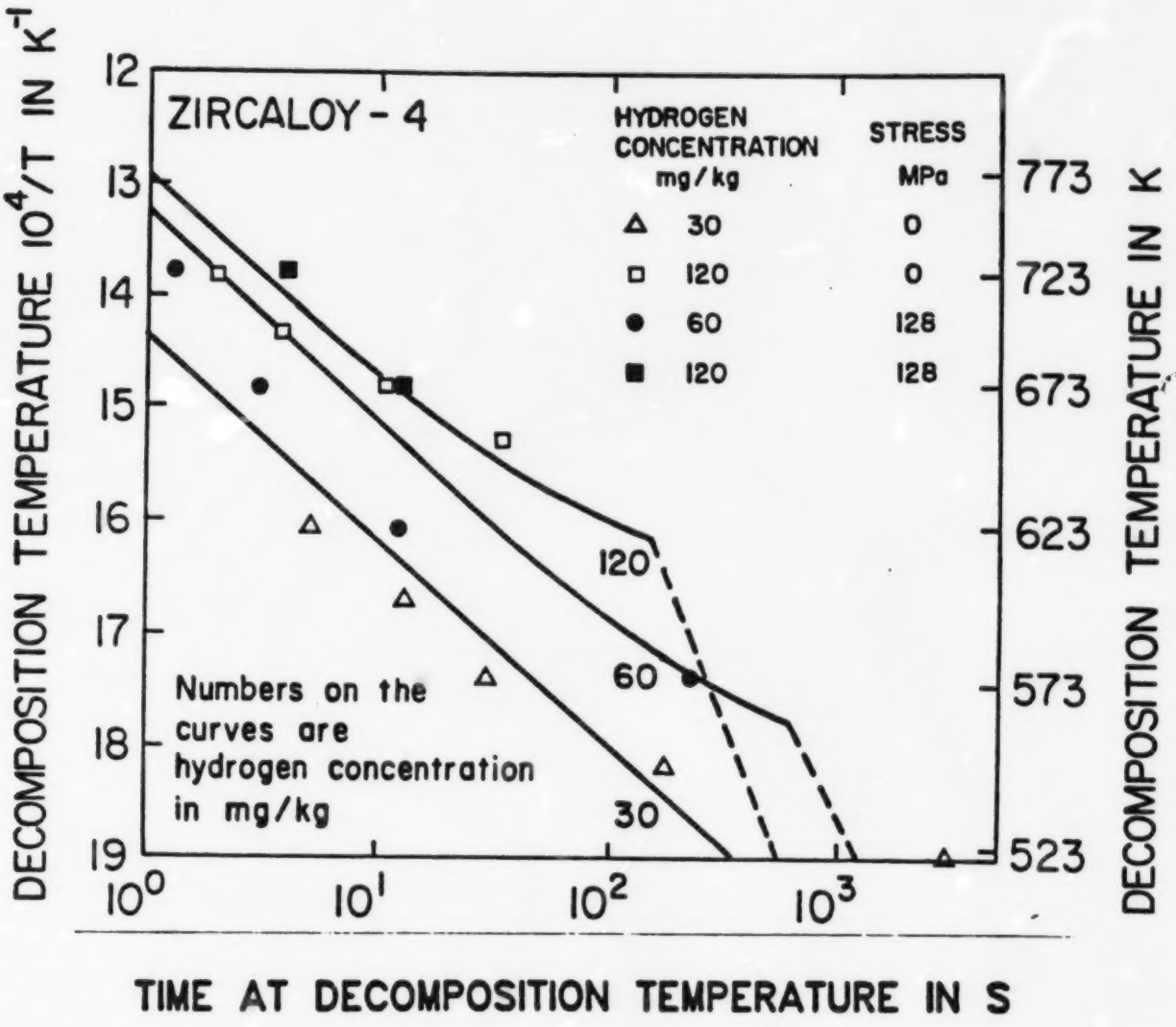


Figure 7: CRNL results: Effect of Temperature, Hydrogen Concentration and Stress on the Isothermal Decomposition of Hydrides in Zircaloy-4. The lines are drawn from Kearns' model [10]

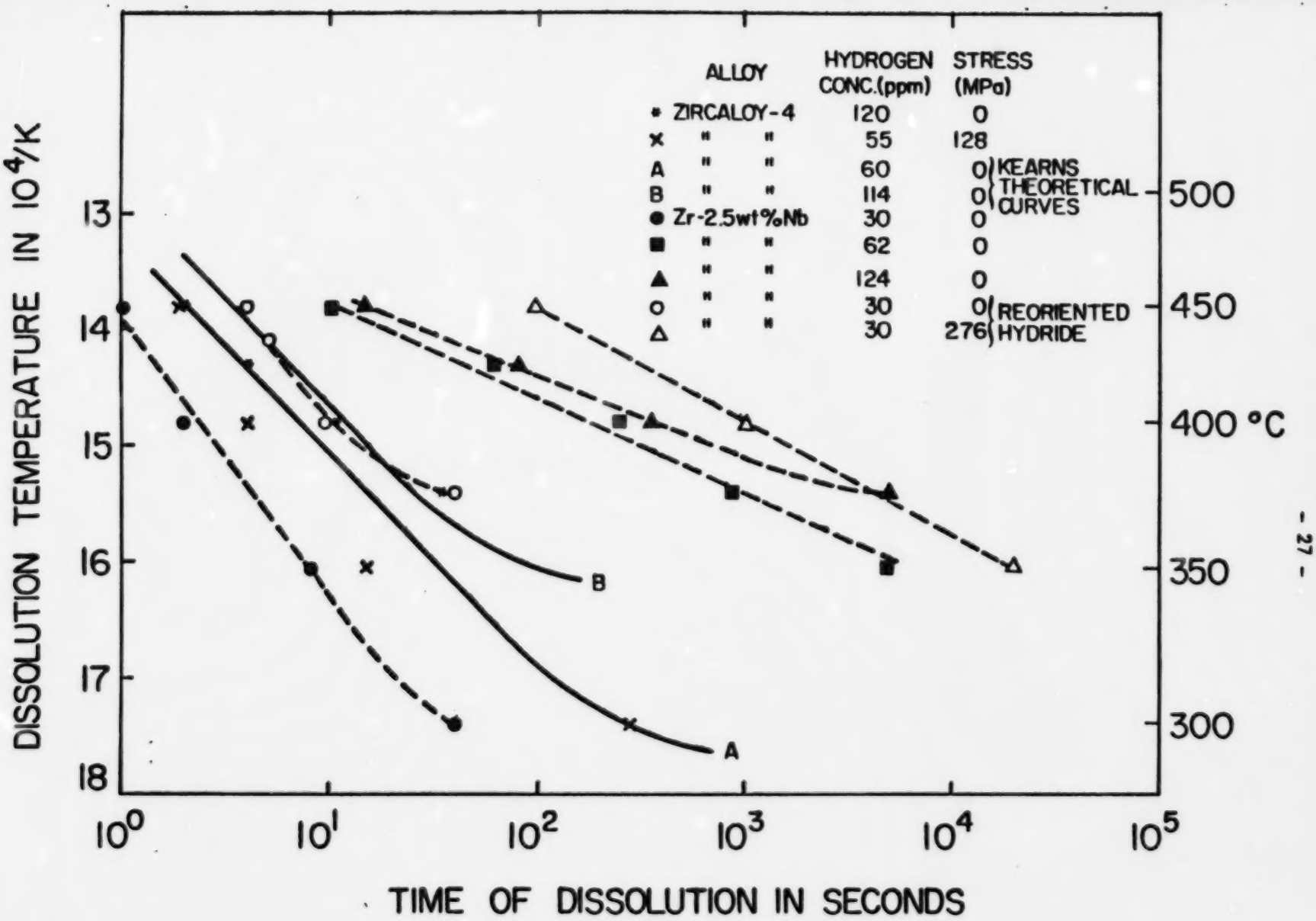


Figure 8: Effect of Temperature and Stress on Hydride Dissolution Rate in Zr-2.5% Nb and Zircaloy-4

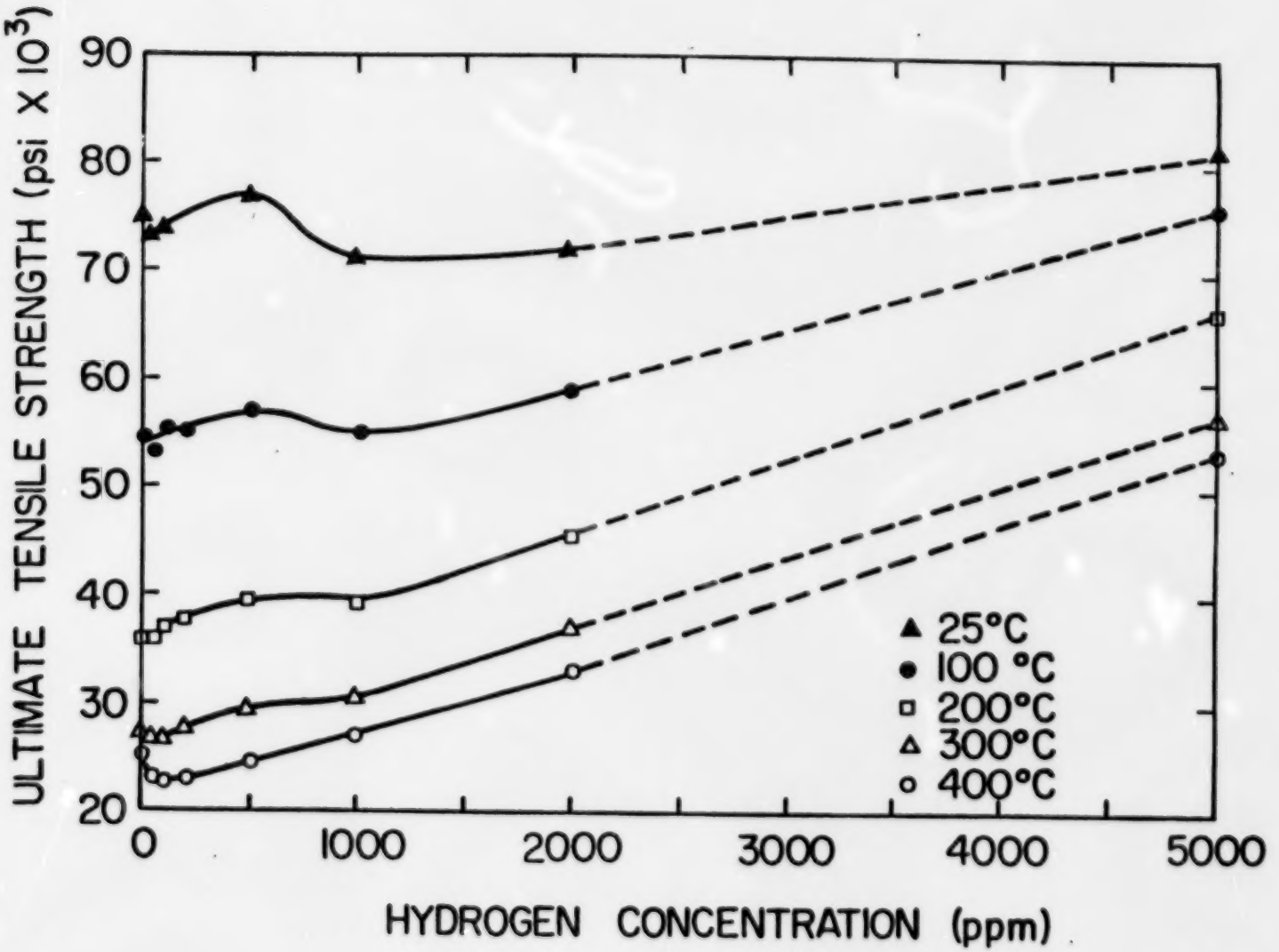


Figure 9: Ultimate Tensile Strength of Zircaloy-2 as a Function of Hydrogen Concentration and Temperature

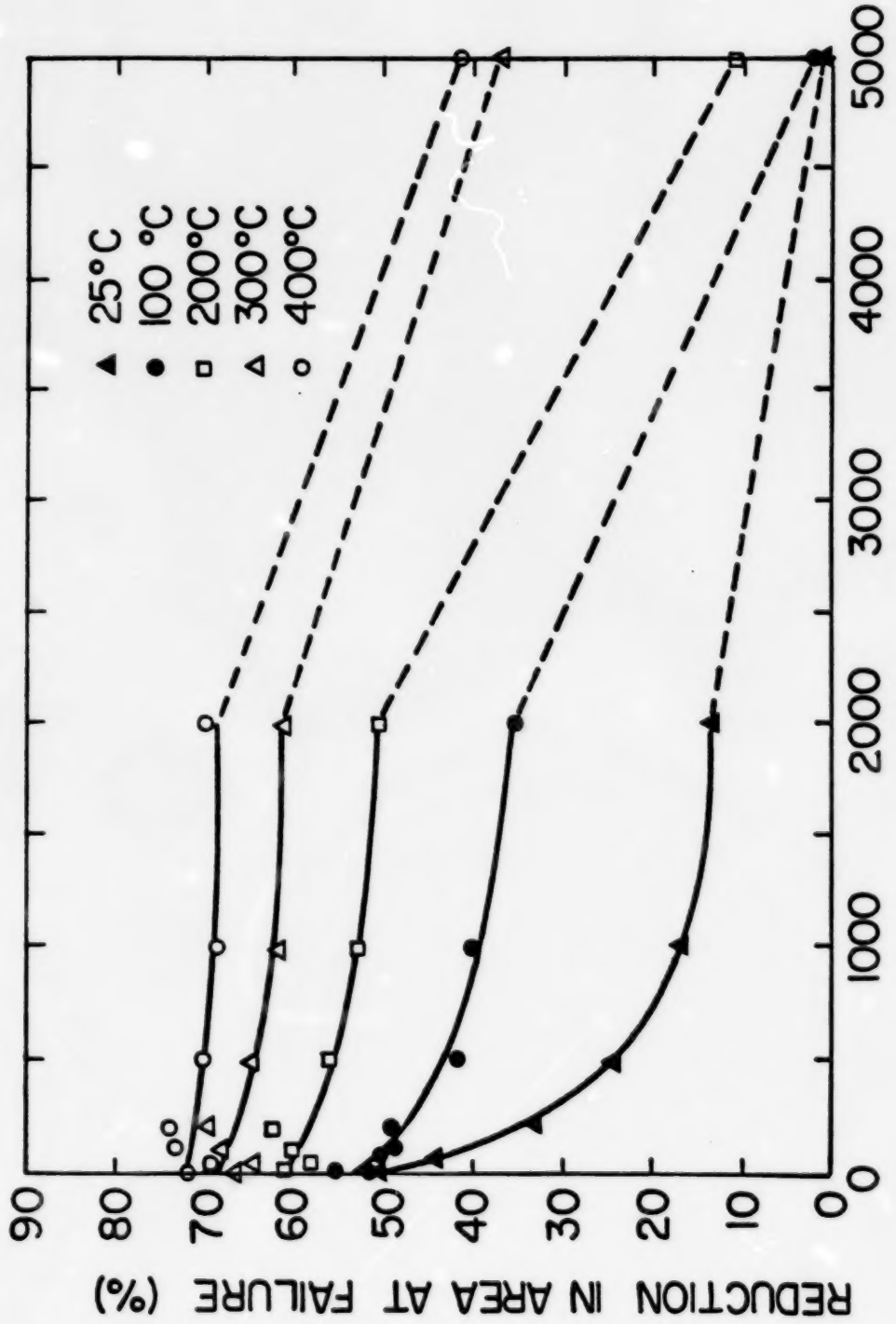


Figure 10: Percent Reduction in Area at Failure of Zircaloy-2 as a Function of Hydrogen Concentration and Temperature

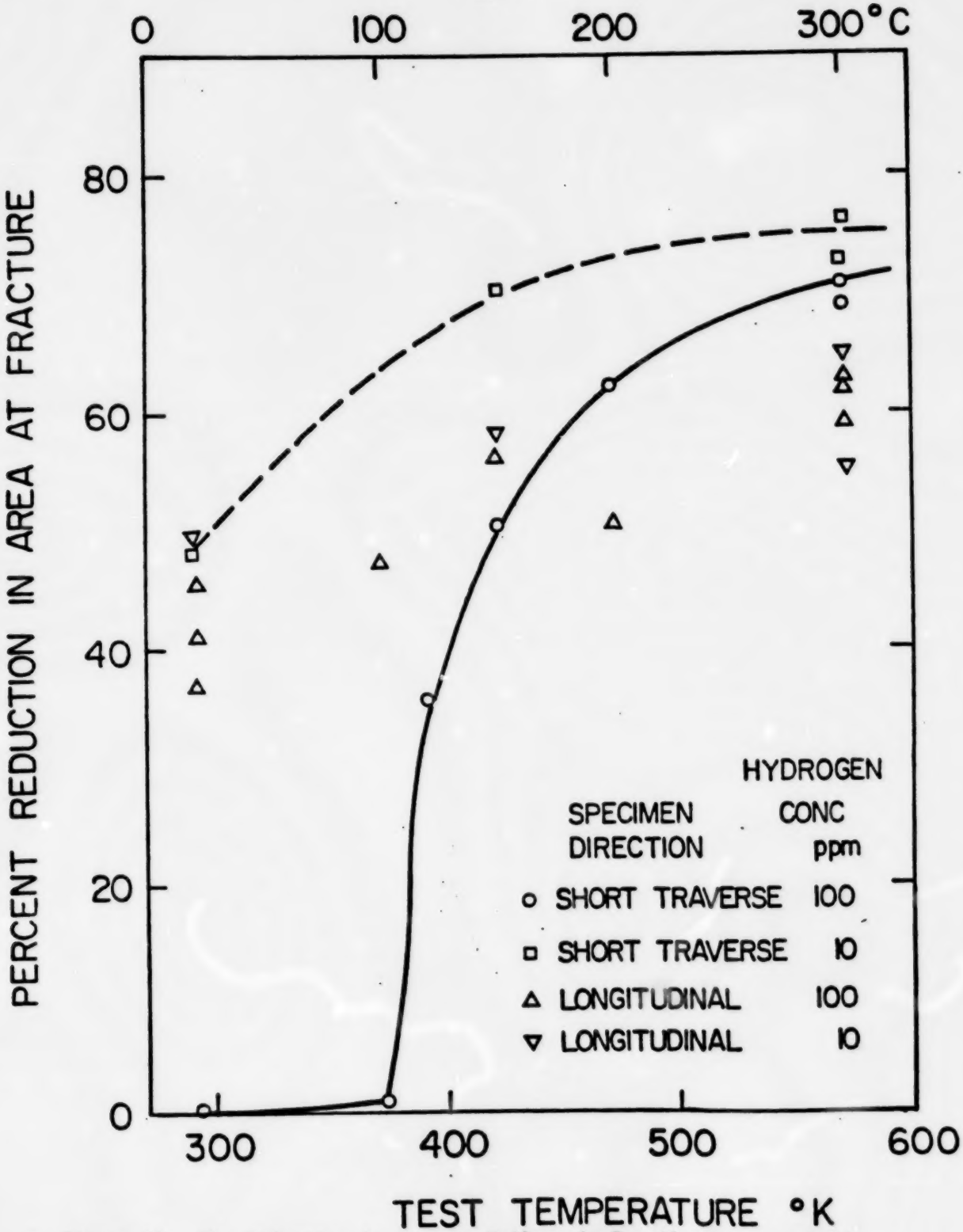


Figure 11: The Reduction in Area at Failure in Tensile Tests on Cold-Worked Zr-2.5% Nb. Specimens were cut from thick sheet in the direction shown; the short transverse specimens had hydride plate normals aligned with the tensile axis.

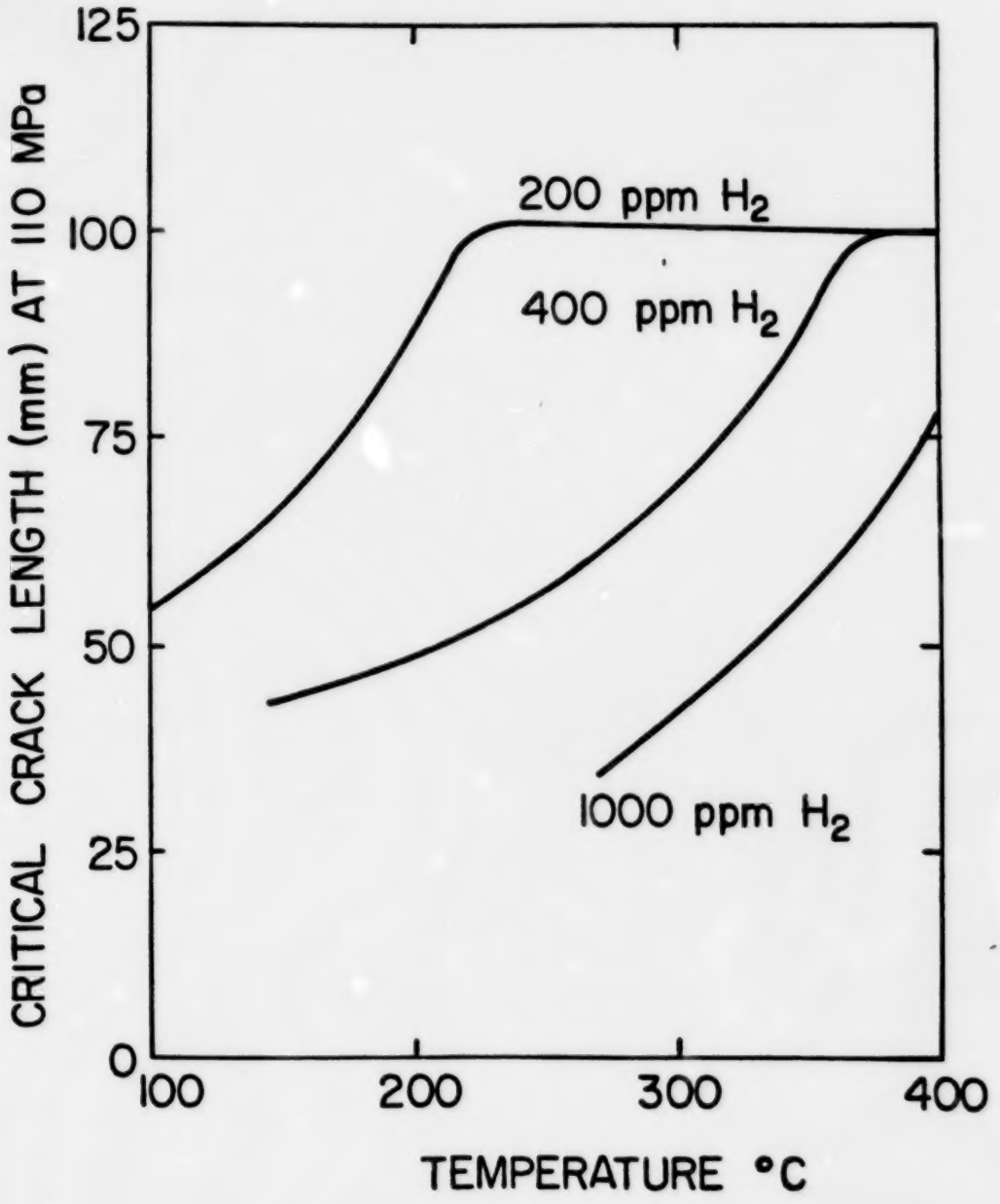


Figure 12: Best Estimate of the Critical Crack Length for Hydrided WSGHWR Pressure Tubes after Irradiation (From 1986 Assessment)

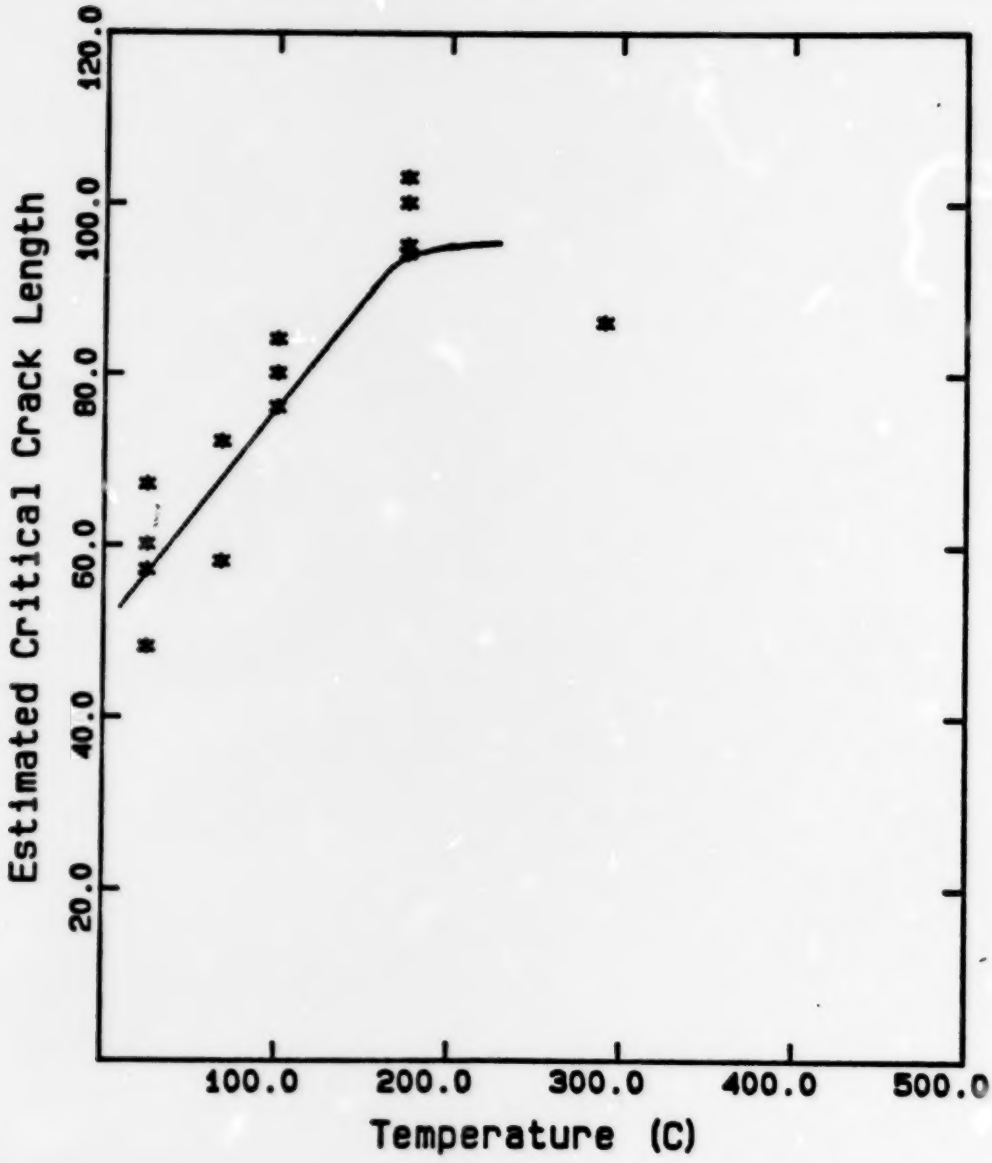


Figure 13: Estimated Critical Crack Lengths in Zr-2.5% Nb Pressure Tubes, Obtained from Compact Tension Specimens Containing 400 to 800 ppm [H]

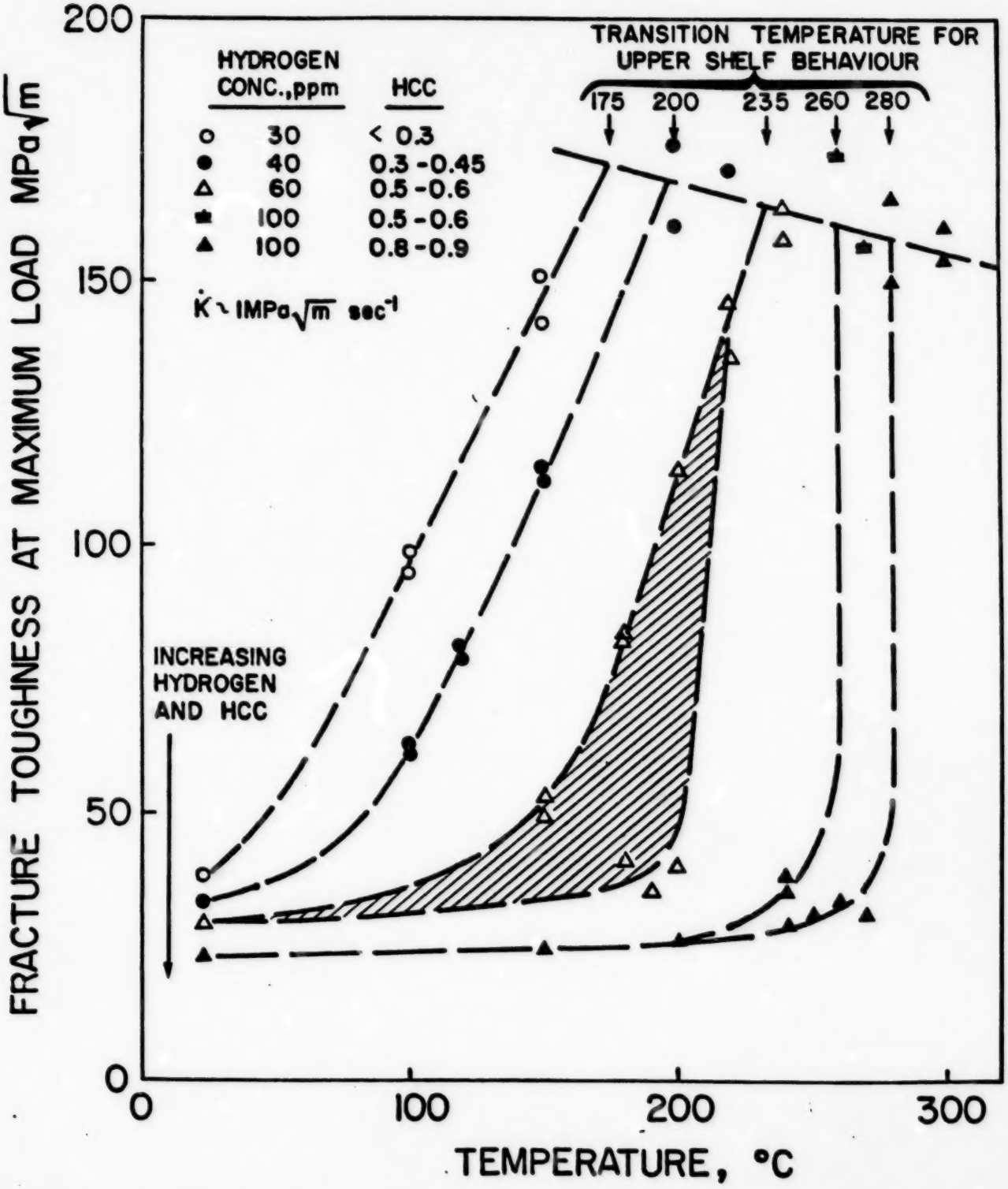


Figure 14: Fracture Toughness Versus Temperature for Zircaloy-2 Compact Specimens with Radial Hydrides: 30 to 100 ppm [8]

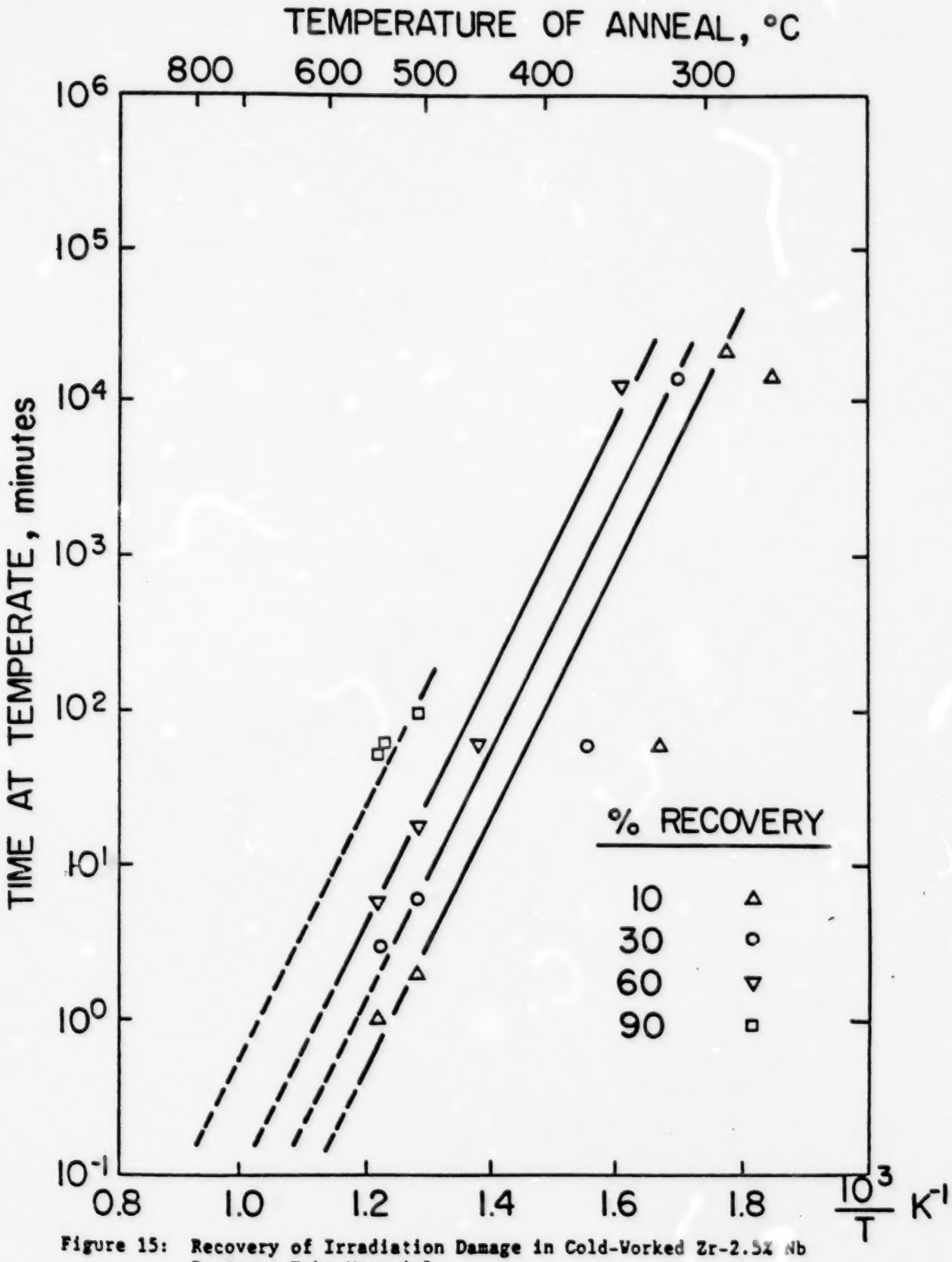


Figure 15: Recovery of Irradiation Damage in Cold-Worked Zr-2.5% Nb Pressure Tube Material

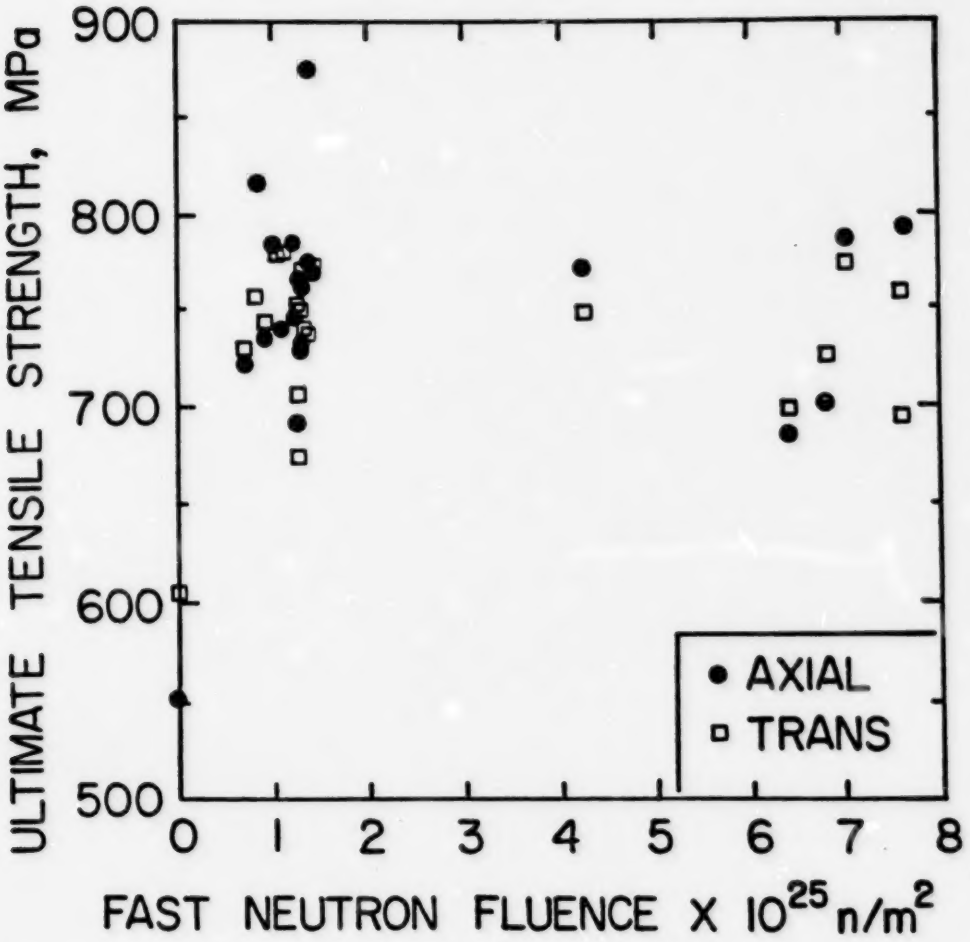


Figure 16: Tensile Strength at 575 K for Specimens from Cold-Worked Zr-2.5% Nb Pressure Tubes Removed from CANDU Reactors

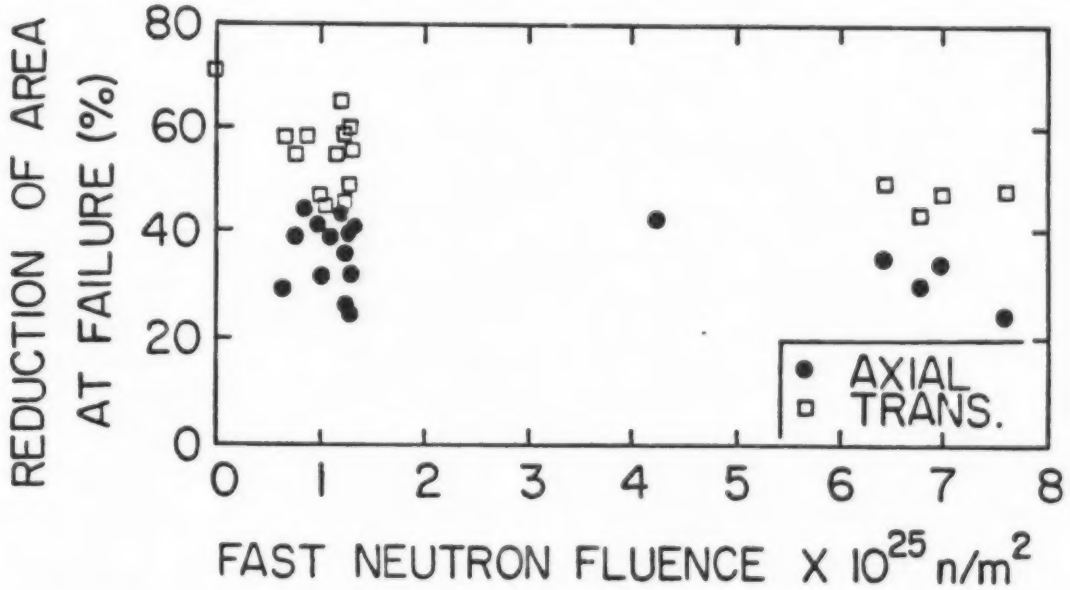


Figure 17: Reduction in Area at Failure at 575 K for Specimens from Cold-Worked Zr-2.5% Nb Pressure Tubes Removed from CANDU Reactors

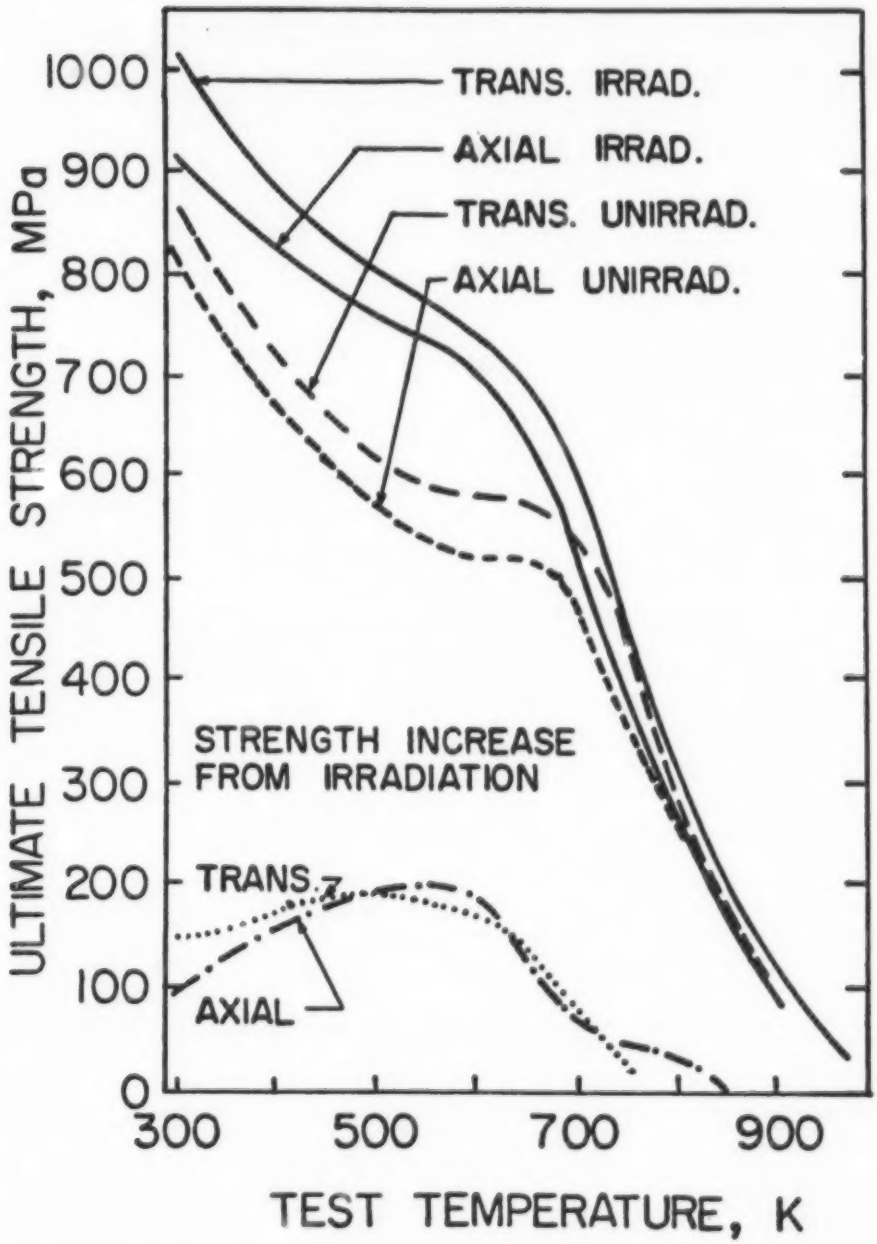


Figure 18: Tensile Strength of Cold-Worked Zr-2.5% Nb with and without Irradiation. Fluence was 1.25 x 10^{25} n/m^2 at 575 K in Pickering 4.

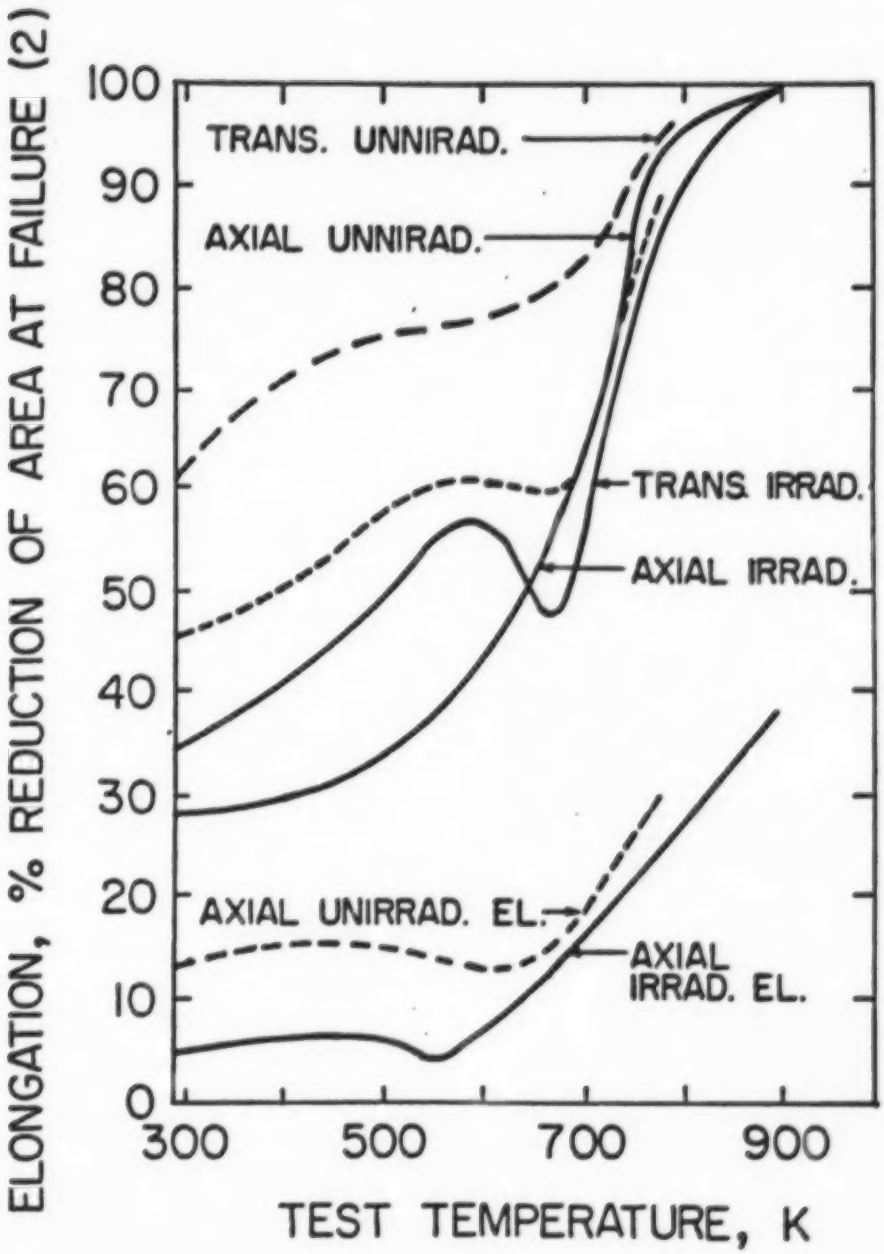
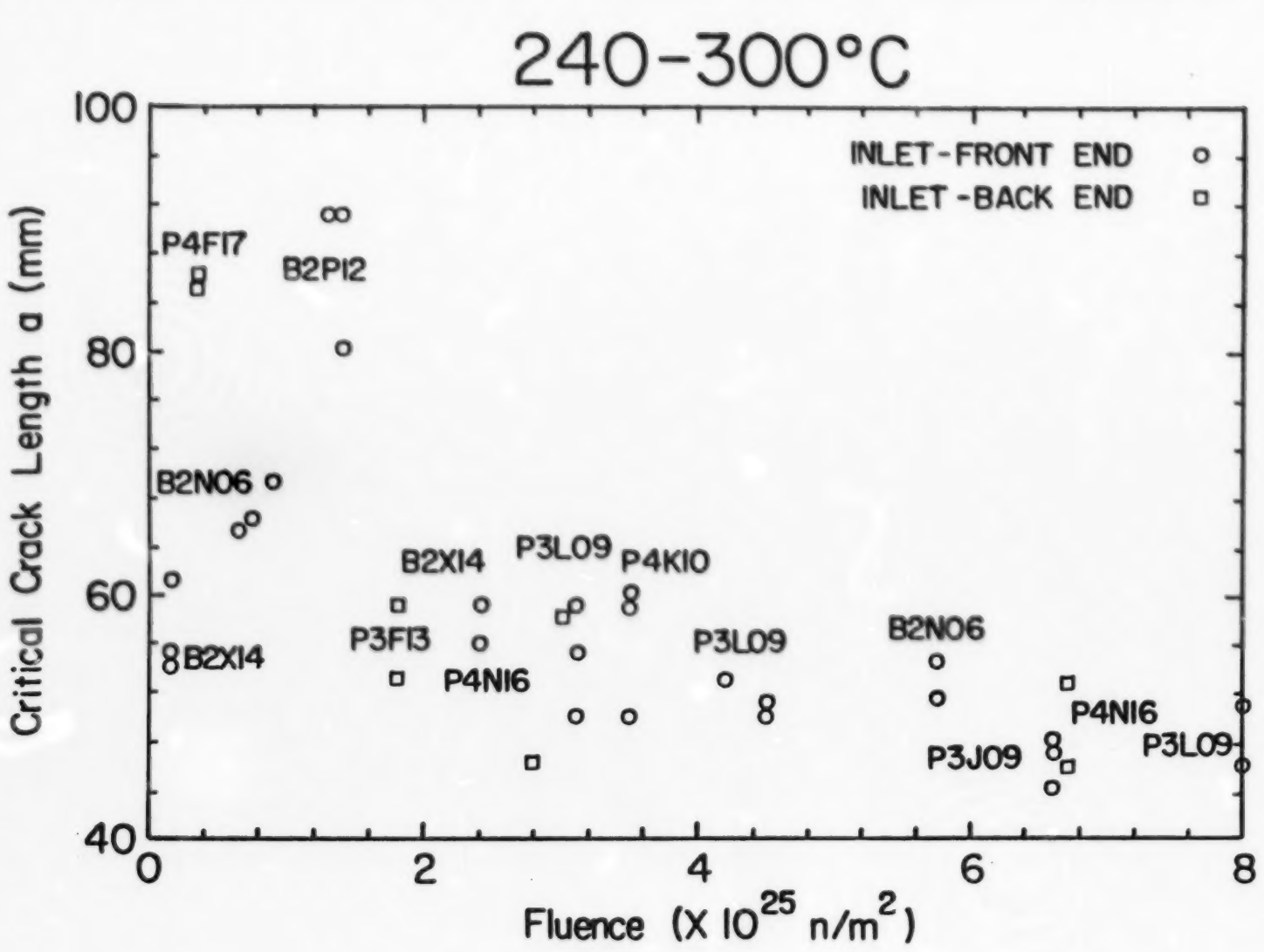
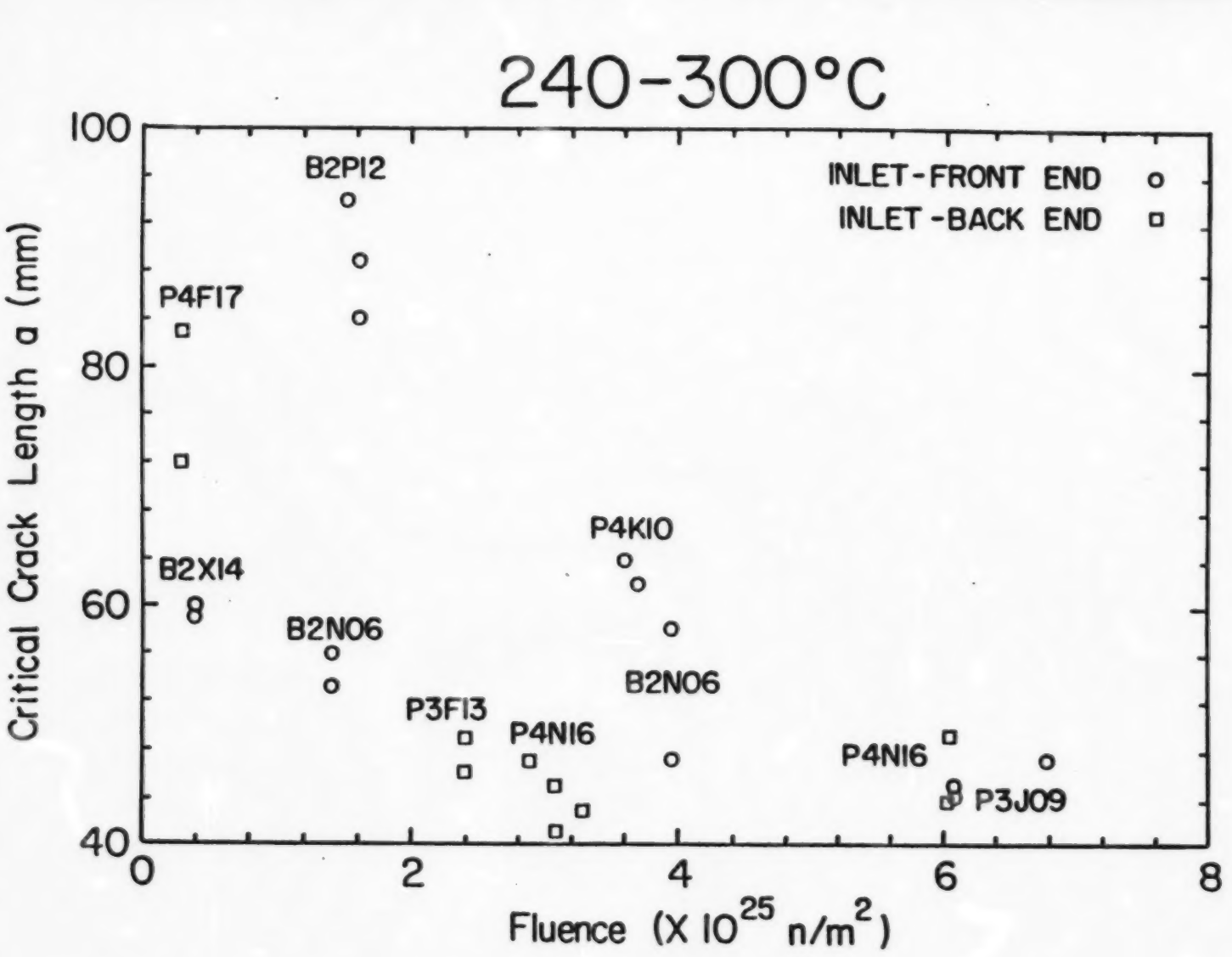


Figure 19: Ductility of Cold-Worked Zr-2.5% Nb Pressure Tubes with and without Irradiation. Fluence was 1.25×10^{25} n/m^2 at 575 K.



Critical Crack Length vs Fluence Outlet End Of Tube

Figure 20: Critical Crack Length Versus Fluence at the Outlet of Pressure Tube



Critical Crack Length vs Fluence Inlet End Of Tube

Figure 21: Critical Crack Length Versus Fluence at the Inlet End of Pressure Tube

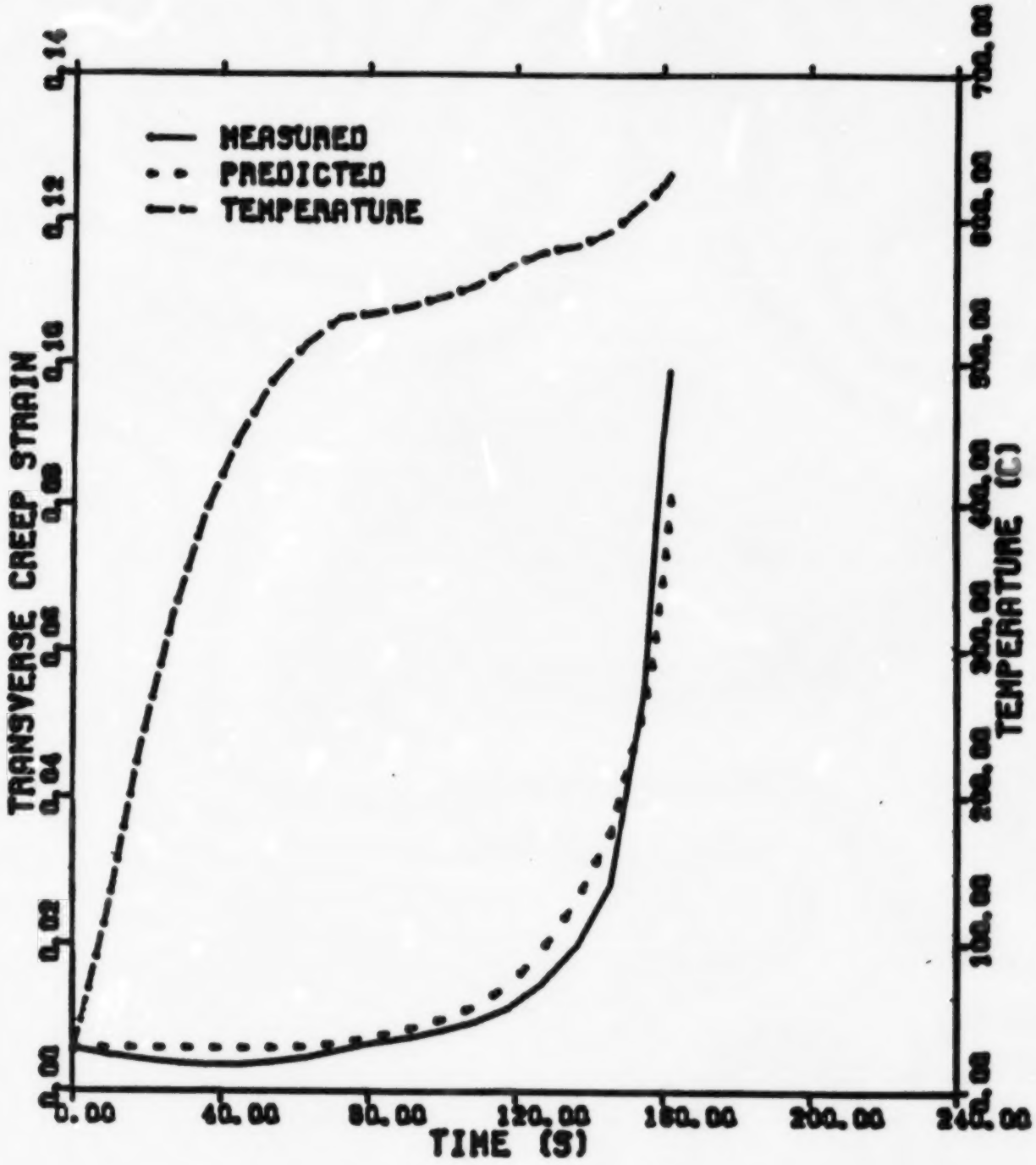


Figure 22: Measured and Predicted Transverse Creep Strain in an Uniaxial Test on an Irradiated Zr-2.5% Nb Specimen. Fluence was $1. \times 10^{25}$ n/m^2 and the average ramp rate was about 4°C/s.

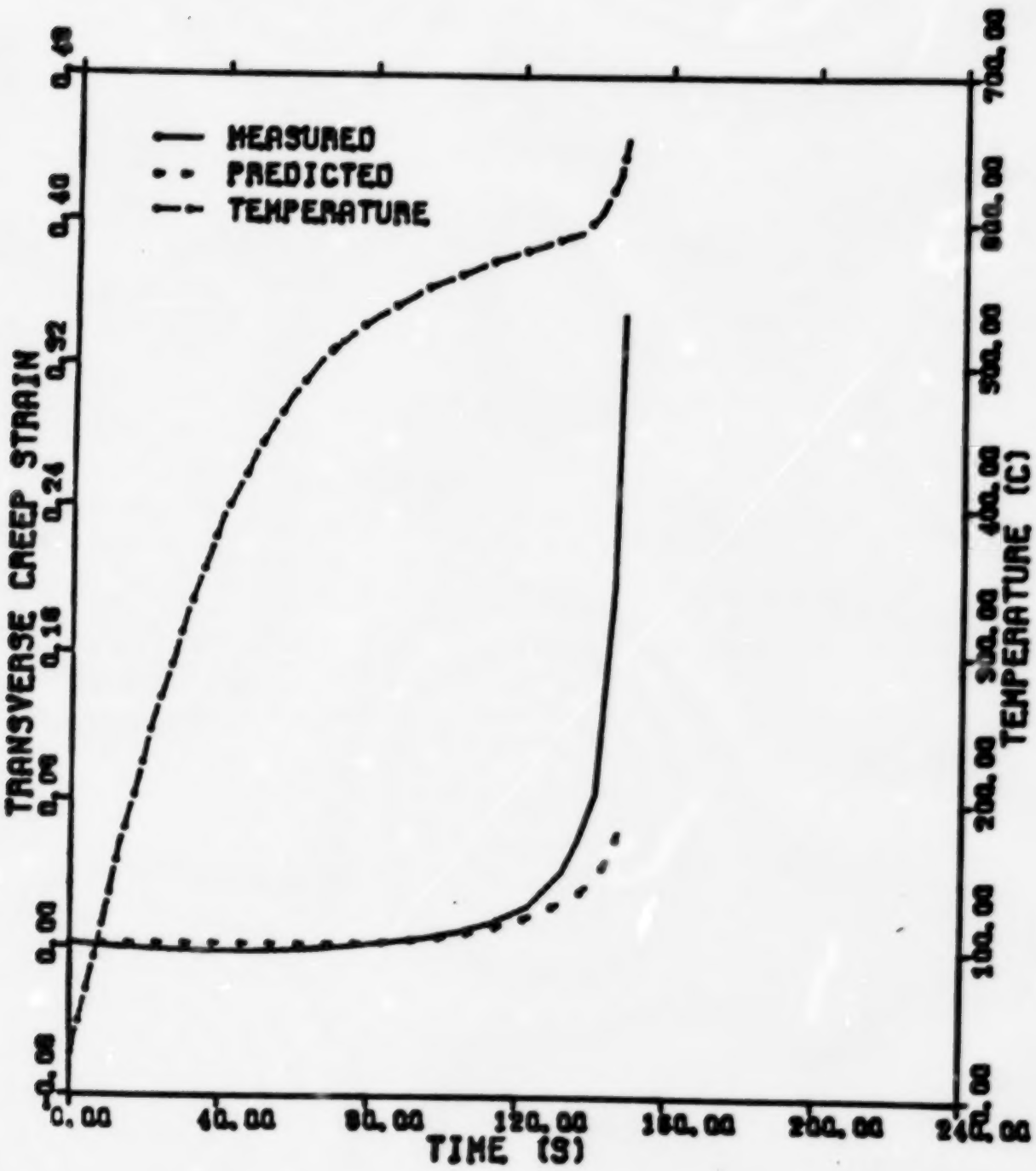


Figure 23: Measured and Predicted Transverse Creep Strain in an Uniaxial Test on an Irradiated Zr-2.5% Nb Specimen. Fluence was 1. x 10^{25} n/m^2 and the ramp rate was about 4°C/s.

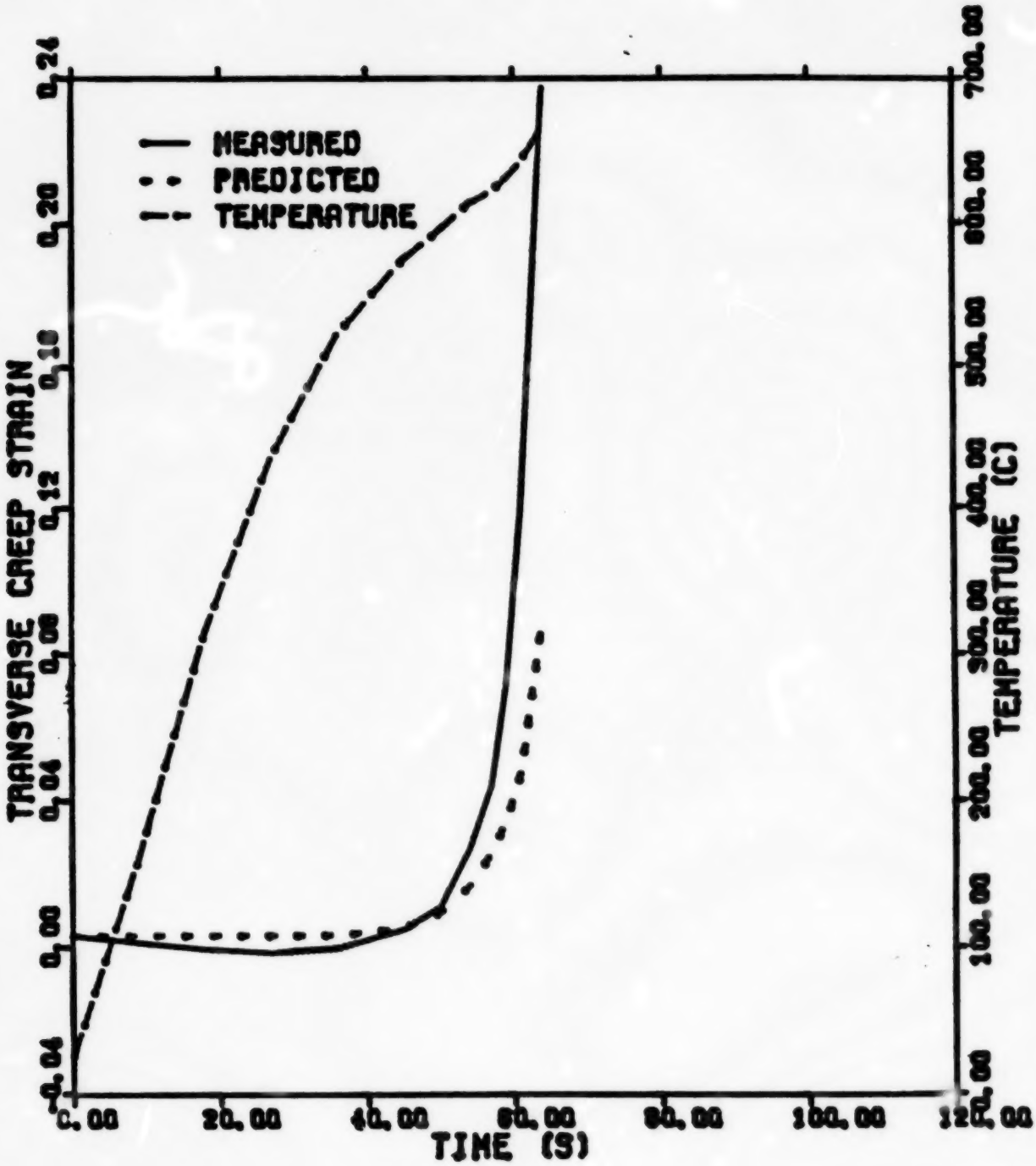


Figure 24: Measured and Predicted Transverse Creep Strain in an Uniaxial Test on an Irradiated Zr-2.5% Nb Specimen. Fluence was $1. \times 10^{25}$ n/m^2 and the ramp rate was about 11°C/s.

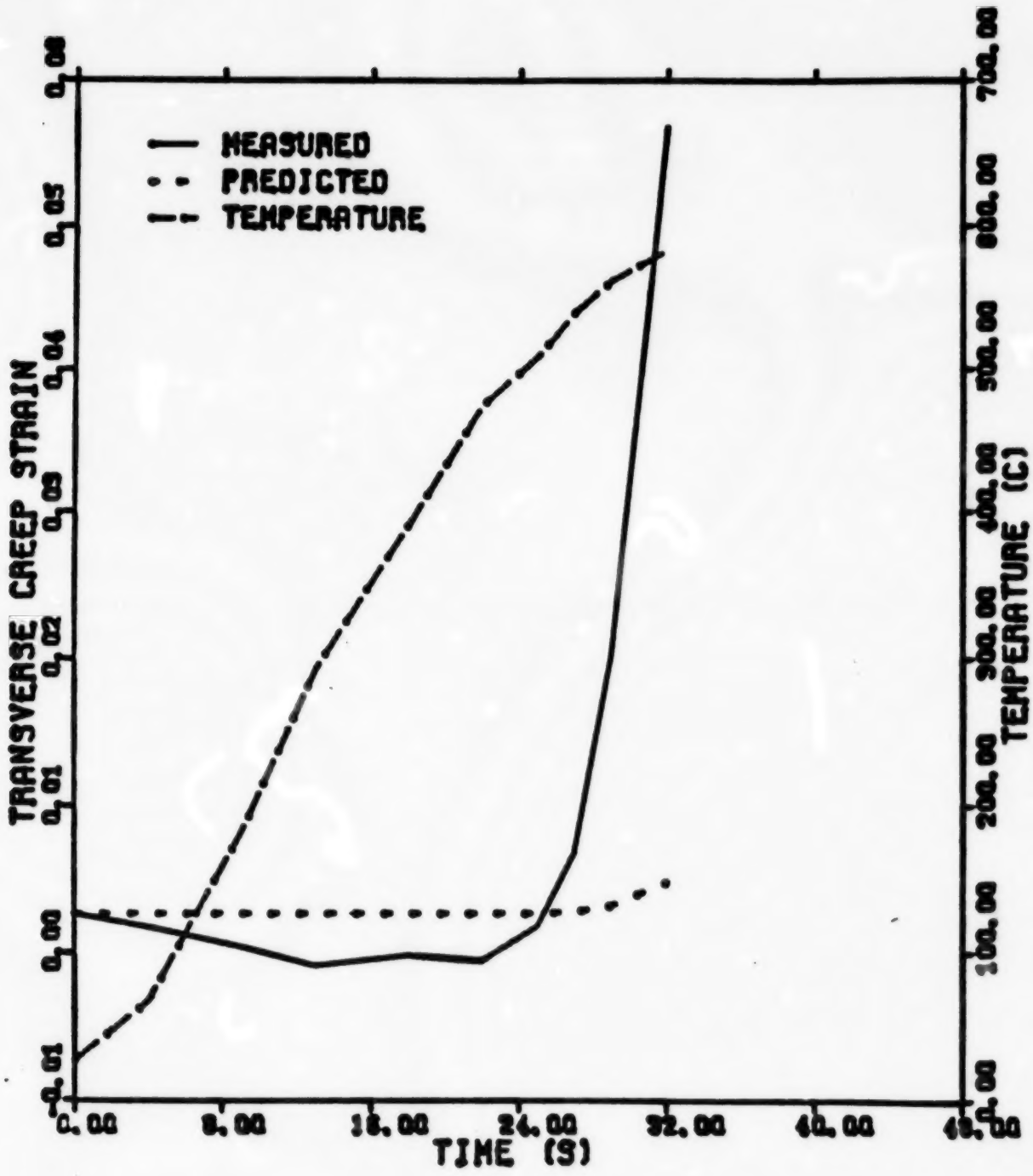


Figure 25: Measured and Predicted Transverse Creep Strain in an Uniaxial Test on an Irradiated Zr-2.5% Nb Specimen. Fluence was 1. x 10^{25} n/m^2 and the ramp rate was about 19°C/s.

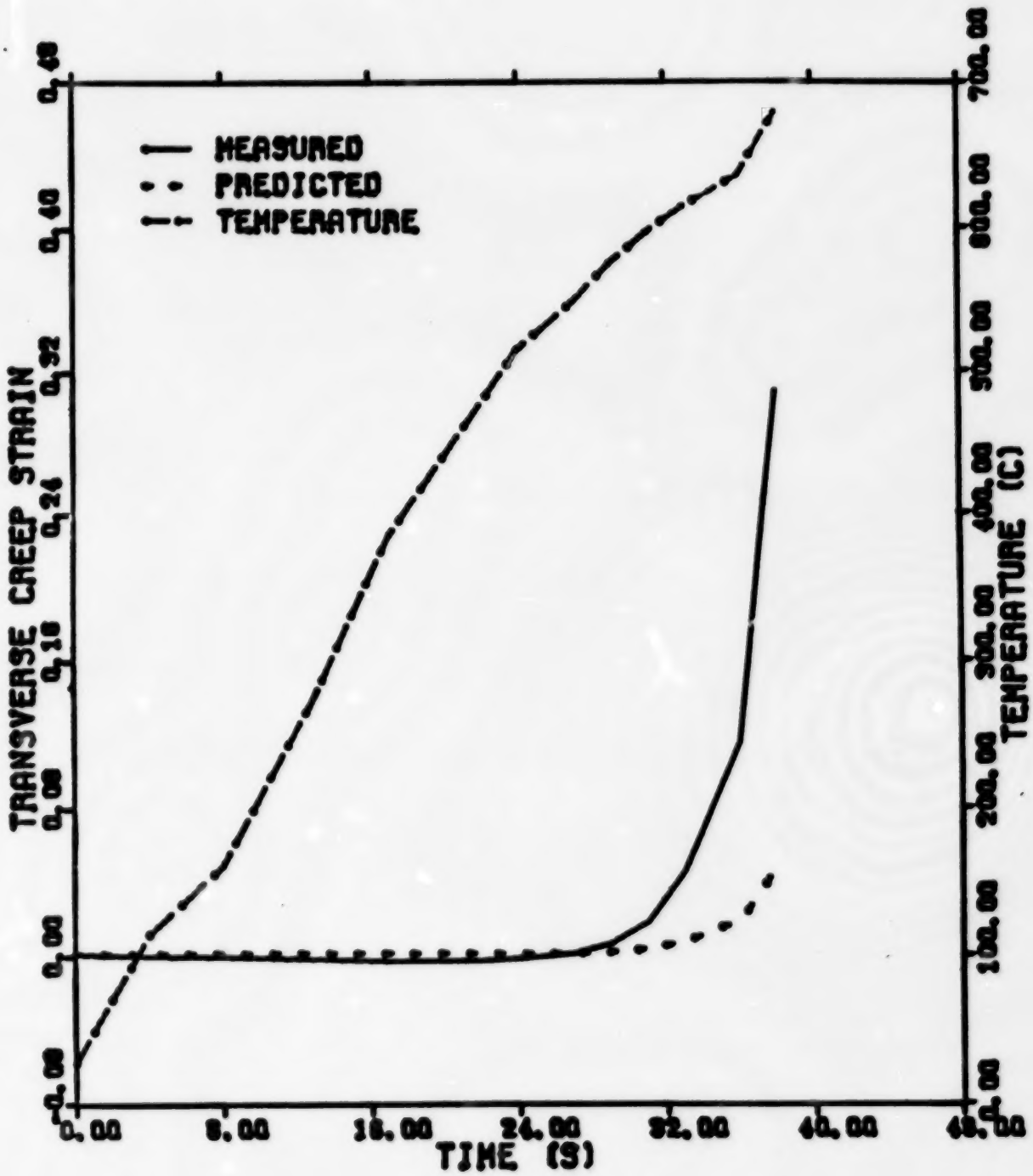


Figure 26: Measured and Predicted Transverse Creep Strain in an Uniaxial Test on an Irradiated Zr-2.5% Nb Specimen. Fluence was $1. \times 10^{25}$ n/m^2 and the ramp rate was about 19°C/s.